



Proceedings

ASPE/ASPEN Summer Topical Meeting

Manufacture and Metrology of Freeform and Off-Axis Aspheric Surfaces

June 26-27, 2014

American Society for

Precision Engineering

P.O. Box 10826, Raleigh, NC 27605-0826
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Proceedings of

Manufacture and Metrology of

Freeform and Off-Axis Aspheric Surfaces

ASPE/ASPEN Summer Topical Meeting

June 26-27, 2014

The Fairmont Orchid
Kohala Coast, Hawaii, USA

The American Society for Precision Engineering (ASPE) is a multidisciplinary professional and technical society concerned with research and development, design, manufacture and measurement of high accuracy components and systems. ASPE activities encompass relevant aspects of mechanical, electronic, optical and production engineering, physics, chemistry, and computer and materials science. Membership is open to anyone interested in any aspect of precision engineering.

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Preface

This book comprises the proceedings of the ASPE/ASPEN 2014 Summer Topical Meeting entitled **Manufacture and Metrology of Freeform and Off-Axis Aspheric Surfaces**. The contributions reflect the authors' opinions and are published as presented to ASPE without change. Their inclusion in this publication does not necessarily constitute endorsement by the ASPE or its editorial staff.

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9:00PM			

Foreword

This joint meeting of the American Society for Precision Engineering (ASPE) and the Asian Society for Precision Engineering (ASPEN) on **Manufacture and Metrology of Freeform and Off-Axis Aspheric Surfaces**, is designed to focus industry, academic and governmental interest and expertise on the advancement of technologies essential to the manufacture of freeform and off-axis aspheric surfaces. The meeting is in conjunction with The Optical Society (OSA) Optical Fabrication and Testing (OF&T) Topical Meeting and Classical Optics Congress, being held at the same location.

Precision freeform (FF) and off-axis aspheric (OAA) surfaces are becoming increasingly present in many application areas—particularly as manufacturing technologies evolve to enable their economical manufacture. Current and emerging examples include optics for astronomy (both space and ground-based), consumer optics (e.g., cell phone camera lenses), automotive (e.g., head and tail lamp lenses), biomedical devices, and a myriad of others. Advances in the manufacture of FF and OAA surfaces are critical to enabling the realization of numerous modern technologies; arrays of individually on axis aspheric elements pose all the same manufacturing problems as freeform optics, and enable technologies such as advanced computational imaging. For the purposes of this meeting, discussion topics will focus on applications where critical dimensions or feature sizes range from the meso- to the macro-scale.

The meeting organizers recognize the critical importance of metrology to the success of precision manufacturing processes; metrology is essential to control of the process, and therefore every bit as important as the process itself. Hence the topic of the meeting, where equal emphasis is placed on manufacturing and metrology.

We encourage presentations of a tutorial nature, which elaborate principles essential to achieving high performance. We aim to bring together specialists and practitioners from industry, government, and academia for the exchange of ideas and to identify topics for further research. The conference schedule includes unstructured time to allow for technical and social interactions.

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Technical Papers

FREEFORM AND OFF-AXIS ASPHERIC SURFACES: METROLOGY CAPABILITIES AND CHALLENGES (Invited)

Chris J. Evans^{1,3} and Angela D. Davies^{2,3}

¹Department of Mechanical Engineering and Engineering Science

²Department of Physics and Optical Sciences

³Center for Freeform Optics

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This talk provides a perspective on the state of technological infrastructure and readiness of dimensional metrology for the full range of freeform and off-axis aspheric optics. We exclude from discussion important optical characteristics such as index, homogeneity, coating thickness, etc.

Metrological infrastructure in a particular topic area may be considered mature when:

- (a) the designer can capture the design intent in unambiguous terms as a specification on a drawing; and
- (b) the metrologist can demonstrate, unambiguously, conformance with the specification using uncertainties evaluated following the ISO Guide to the Expression of Uncertainty (GUM).

There are many disparate definitions of “technology readiness levels” across different US government departments, industry groups and multinational agencies. Here we define commercial readiness as proven technology that can be integrated into an optics manufacturing system and operated with acceptable performance and reliability without additional research and development.

This conference – and hence this paper – covers a very large range of lateral and vertical scales. Laterally (approximately in the plane of the optical surface) the current range spans from the aperture of the primary segments in large telescopes (8.4 m) to the spatial scales of roughness specified for EUV optics (~ 1 nm). Vertically (normal to the plane of the surface) the range goes from the roughness amplitude of EUV optics (0.15 nm rms) to telescope mirror radii of curvature (40 m or greater). This dynamic range of 10^9 or greater ensures that

conformance testing generally requires use of multiple instruments.

For simplicity, we consider dimensional metrology in 3 commonly understood (but not well defined) bands: figure (or form); mid-spatial (waviness) and finish (roughness).

Optical surface finish metrology is mature. Specification of statistically stationary surfaces using rms amplitude and the power spectral density function (PSD) are directly connected to optical function and can be measured using commercially available tools.

Modern methods for manufacturing freeforms and off-axis aspherics typically introduce a signature in the surface related to the tool footprint, often described as mid-spatial frequency errors. A variety of instruments are available to measure such surface components. There has been some recent work on relating such structures to imaging performance. There is no consensus on which mathematical tools should be used to specify them.

A variety of contact and non-contact tools are used for figure metrology. The preference in the optics industry for full aperture interferometry has to be tempered by the reality of the limitations of such techniques. In some application areas, the technology is mature. For some high throughput, large departure freeforms there are no demonstrated solutions.

Fabrication of Grazing Incidence Mirrors for Future X-ray Astronomical Missions

Raul E. Riveros and William W. Zhang
X-ray Astrophysics Laboratory
NASA Goddard Space Flight Center
Greenbelt, Maryland, USA

INTRODUCTION

Scientific observations of X-ray radiation from cosmic sources have greatly advanced our understanding of the universe and the physics it follows. For the past ~50 years, researchers have attempted to build high-throughput and high-resolution X-ray telescopes. Improvements in X-ray telescope technology have always resulted in greater scientific knowledge. An X-ray telescope's performance is generally measured by two critical factors: angular resolution and photon collection area (effective area). In practice however, these two metrics are at odds with each other due to the unique geometry of X-ray optics.

X-ray photon wavelengths are approximately 1000 times smaller than those of visible photons and occupy a longer range of wavelengths (100 nm to 0.01 nm). As a result, X-ray photons exhibit practically no refraction and are easily absorbed by most materials, rendering refractive and normal-incidence reflective optics unusable for imaging X-rays. In 1952, Hans Wolter proposed a set of X-ray optic designs which employ grazing-incidence mirrors that take advantage of total external reflection (incidence less than the critical angle) [1]. The most widely used of these designs is the Type-I, shown in Figure 1(a). As shown, mirrors are built to conform to the paraboloidal and hyperboloidal surfaces. Incident rays reflect off the mirror surfaces and focus. As such, both the primary and secondary surfaces may be thought of as "extreme" off-axis mirrors. Since their angle relative to the optical axis is so steep, the photon effective area is small; to increase this effective area, several confocal telescopes are arranged as shown in Figure 1(b). Furthermore, this "nested" Wolter type-I design requires thin mirrors, as thick mirrors would occult the aperture; it is this requirement for thin mirrors and the X-rays' small wavelength that makes X-ray mirror manufacturing a highly challenging task.

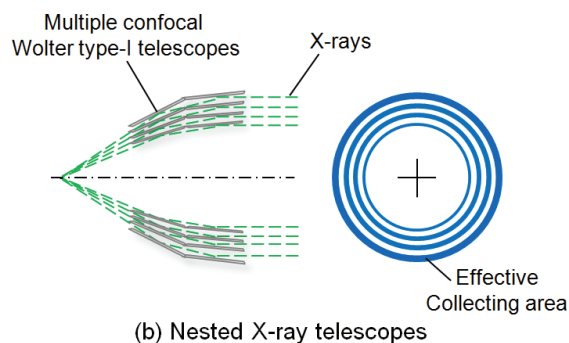
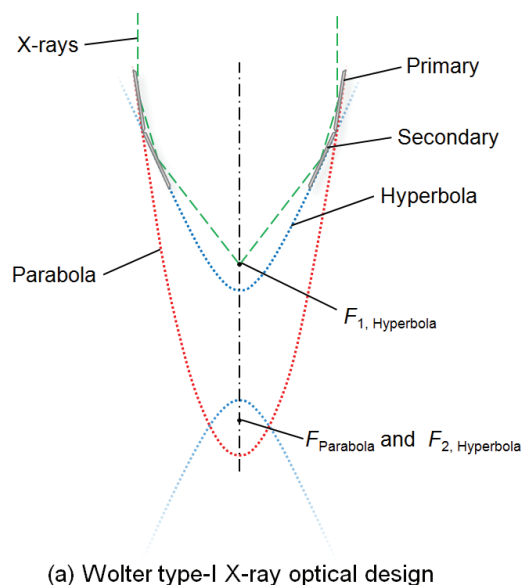


Figure 1. Design of X-ray optics.

X-ray photons are absorbed by the earth's atmosphere thereby requiring all X-ray telescopes to be space borne. The reality of spaceflight imposes limitations on mission cost, weight, volume, and production time; these limitations, along with scientific drivers for the mission, have forced previous and current missions to focus on maximizing a single performance factor [2]. For instance, the Suzaku X-ray observatory is optimized for high effective area per unit mass by nesting 175 conically approximated mirror shells within a 400 mm

diameter aperture [3]; the XMM-Newton observatory exhibits a large effective area ($\sim 0.45 \text{ m}^2$ at 1 keV) [4]; the Chandra X-ray observatory features superbly precise mirrors with an unsurpassed angular resolution of 0.5" half power diameter (HPD) [5].

To succeed scientifically, future X-ray telescopes cannot sacrifice angular resolution for effective area or vice versa; they must maximize both within a given mission budget. Modern mirror fabrication technologies cannot meet all the requirements for future X-ray observatories, and new technologies and methodologies are needed. This paper therefore aims to delineate mirror fabrication requirements for future X-ray missions and to present our mirror fabrication efforts at Goddard Space Flight Center (GSFC).

NEXT GENERATION X-RAY OBSERVATORIES

Several X-ray observatory mission concepts have been proposed by the scientific community, which require X-ray optics with unprecedented angular resolution and effective area [6-8]. In general, future X-ray missions will require between 1 and 3 m² of effective area and an angular resolution of 1 arcsec or better. The completed spacecrafts need to fit within the volume constraints of existing rockets, generally limiting their entrance aperture to $\sim 3 \text{ m}$ or less. To achieve specified effective areas within the aperture limits, future X-ray telescopes will have $\sim 1,000$ nested paraboloidal and hyperboloidal mirror shells which are $< 1 \text{ mm}$ thick. Producing large-diameter thin mirror shells is not practical. Instead all mirror shells are to be assembled from mirror segments having an area of approximately $200 \times 200 \text{ mm}$ or less. Using this segmented approach results in X-ray mirror assemblies comprised of 8,000-15,000 mirror segments.

There are many significant foreseeable challenges associated with realizing a mission with a segmented mirror assembly of this size which achieves the required angular resolution of $< 1 \text{ arcsec}$. A large fraction of these challenges lies in the production of so many mirror segments which are accurate enough to both reflect X-rays efficiently and with minimal scattering by having excellent microroughness, and when assembled, focus images to 1 arcsec. Additionally, these mirrors must be produced within a mission's allotted production timeframe

(3-5 years) and within the specific mirror production budgets, which can range between \$50M and \$150M. This sets the cost of a single mirror segment to \$5K-\$18K, including material costs.

Mirrors segments for a future X-ray telescope have nearly cylindrical surfaces whose axial profiles follow a "freeform" optical prescription which deviate $\sim 1 \mu\text{m}$ from a straight profile. The accuracy of the mirror segments in the axial direction is crucial to the performance of the telescope. The telescope's overall angular resolution of 1 arcsec must include error contributions from the mirror coating, alignment, and bonding of the mirror segments to the spacecraft structure and the surface quality of the mirrors themselves. One example might be a mirror surface error contribution of 0.5 arcsec. Mirror surface quality requirements for the axial figure of a primary and secondary mirror pair capable of focusing to 0.5 arcsec are shown in Table 1. The surface quality of excellent X-ray mirrors must be controlled over ~ 6 spatial frequency decades (200 mm to $1 \mu\text{m}$), since X-ray photons are susceptible to smaller surface variations than optical or UV light. It is possible to produce such mirror surfaces using modern optical manufacturing technology [9]. It is also possible to produce such mirror surfaces on thin ($< 1 \text{ mm}$) substrates as well using specialized manufacturing technology. Our investigations, however, suggest that producing 8,000-15,000 excellent yet thin mirror substrates within 3-5 years at \$5K-\$18K per mirror is not possible with any set of commercially available processes.

Table 1. Mirror and production requirements for a 0.5 arcsec mirror.

Error type	Spatial wavelength band	Specification
Axial figure	5 to 200 mm	Slope $< 0.1 \text{ arcsec}$
Axial mid. frequency	1 to 5 mm	$< 0.5 \text{ nm}$
Microroughness	$1 \mu\text{m}$ to 1 mm	$< 0.3 \text{ nm}$

Realizing a true high-performance next generation X-ray telescope would therefore require new mirror production technologies that simultaneously achieve high speed, high accuracy, high repeatability, and low cost.

MIRROR FABRICATION EFFORTS AT GSFC

Our group at GSFC is dedicated to finding and developing processes to meet the unprecedented manufacturing challenges of constructing the next generation of X-ray telescopes. We use two distinct the mirror segment fabrication methods: precision thermal slumping of commercially available thin glass sheets and polishing and light-weighting single crystal silicon.

Precision Glass Slumping

Glass slumping involves high-precision mandrels (1-2 arcsec) and commercially available thin glass sheets. The flat glass sheet is balanced on the curved mandrel and heated until the glass softens and conforms to the mandrel's surface shape. This process is illustrated in Figure 2. Glass slumping features several attractive advantages. The glass material is relatively inexpensive and is commercially available in high quality and volume. Additionally, the glass arrives from the manufacturer with an excellent 0.3 nm microroughness; careful tuning of the heating cycle parameters prevents degradation of this microroughness throughout the slumping cycle.

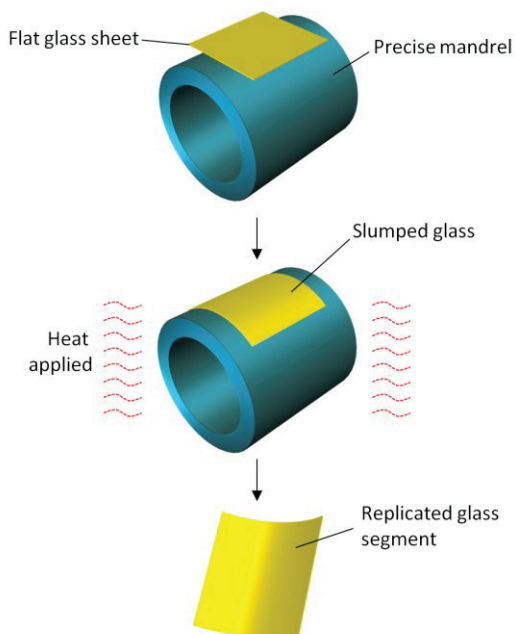


Figure 2. Schematic of the precision slumping process used to produce glass X-ray mirror segments.

Our glass slumping process has been developed for the past 10 years, resulting in a

steady improvement in mirror segment angular resolution from ~60 arcsec to ~6 arcsec [2]. Glass slumping was used to produce mirror segments of the NuSTAR X-ray mission launched in 2012. It appears that slumped glass mirrors segments cannot be reliably produced to less than 5 arcsec. This limitation comes from the sprayed boron nitride release layer thickness distribution. These variations create mid-spatial frequency ripples that contribute at least ~4 arcsec in figure error. Current and future developmental efforts will focus on increasing the process yield and reducing operating costs.

Single Crystal Silicon Polishing and Light-weighting

There are several advantages to using single crystal silicon as a mirror substrate material. Firstly, it is abundant and inexpensive when compared to optical glass since it is produced for the semiconductor industry. Single crystal silicon is commercially available and virtually free of internal stress since the silicon atoms reside in their lowest energy crystal lattice position (unlike glass which has an amorphous and innately stressed atomic structure). The processes for manufacturing high quality single crystal silicon ingots are mature and improving. Furthermore, auxiliary processes like slicing, dicing, etching, coating, grinding, lapping, and polishing are also well established for applications to single crystal silicon.

Fabricating X-ray optics made of single crystal silicon has been attempted in a variety of ways previously [10, 11]. A new approach which aims to fabricate lightweight mirrors substrates using single crystal silicon was conceived in 2011 [12]. This method takes advantage of single crystal silicon's stress free nature by using well established silicon processing technologies to either maintain or recover the mirror segment's stress free state. Figure 3 shows this process concept.

The process begins with the growth of a single crystal silicon boule by the Czochralski process (or a suitable alternative) to a size capable of accommodating the largest mirror segments needed from that particular boule. The boule is then sliced to produce rectangular blocks. Using either a wire-based or other material removal process, the mirror shape is generated on the block. The slicing and form generation operations (wire saw, wire electro-discharge machining (WEDM), etc.) leave a mechanically

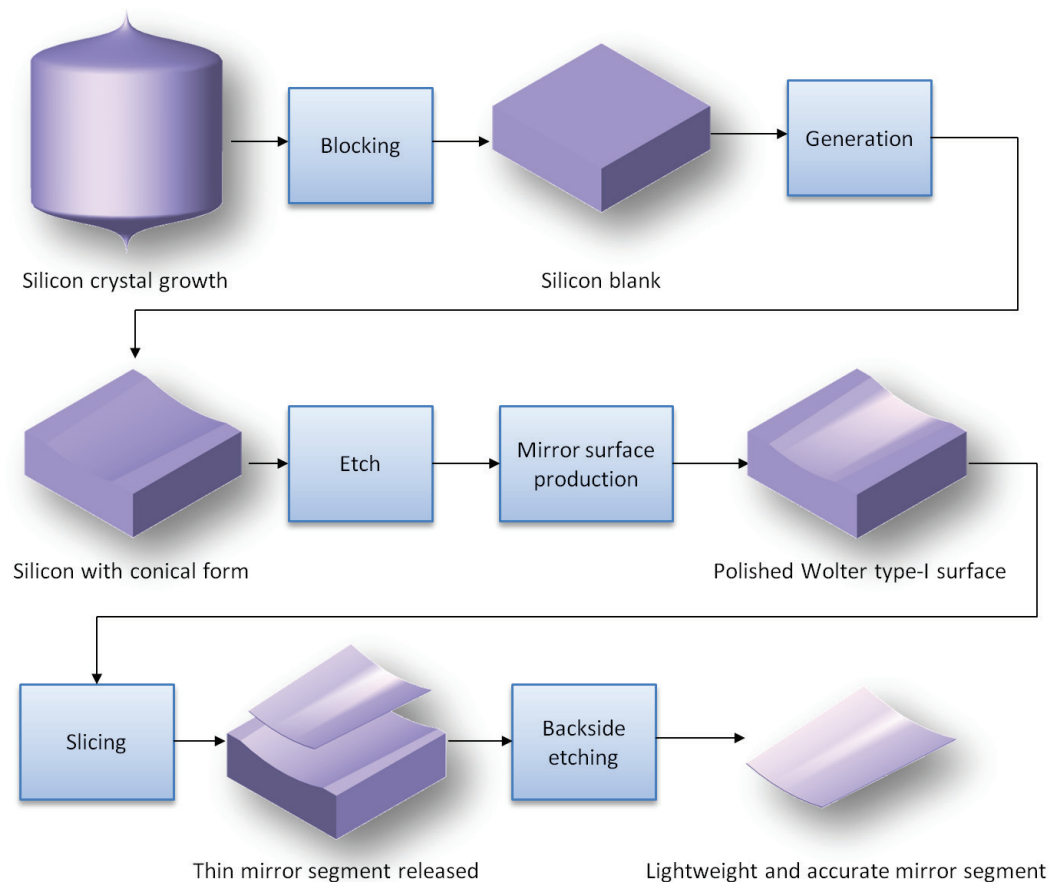


Figure 3. Conceived process flow for the production of single crystal silicon mirror segments.

damaged and chemically modified surface layer; therefore, the block is etched in an isotropic etching solution to remove the damage layer and expose the underlying silicon crystal. The block then undergoes a series of processes (e.g., grinding, etching, polishing, metrology, etc.) which yields a highly accurate damage-free mirror surface. The block is cut sufficiently thick such that nearly any optical finishing and metrology process can be applied to the mirror surface, minimizing concerns of workpiece deflection or frailty.

Once polished, the mirror undergoes a string of processes termed “light-weighting.” A wire cutting process is used to slice off the mirror surface to a set thickness rendering a lightweight but deformed (due to damage layer stress) mirror segment. The polished mirror surface is then coated with a removable protective layer and submerged in an isotropic etching solution to remove the damage layer and expose the underlying crystal on the mirror’s backside. The protective layer is removed, revealing a mirror segment that is a perfect

stress-free single crystal that has retained the mirror figure polished into it earlier, provided the polishing processes left a totally damage free surface. Damage free polishing can result from surface finishing processes which use chemical action to remove surface atoms as opposed to mechanical methods of material removal.

This production process promises lightweight X-ray mirror segments with unprecedented performance. However, its feasibility must be demonstrated. As a starting point, flat mirrors were produced using some manifestation of this procedure in 2013. Results from these trials indicated that the light-weighted flat and round mirror (4 in diameter) preserved its figure well [2]. Efforts are underway to produce cylindrical single crystal mirrors currently, and this process will hopefully be applied to Wolter type-I mirrors in the coming year. Many challenges are associated with this process and the fabrication of Wolter type-I mirrors as well. In addition to perfecting our methodologies of each process step, our research efforts are focused on determining a mirror figuring process capable of

producing an excellent X-ray mirror surface quickly and inexpensively; this will surely be one of the greatest challenges of realizing single crystal silicon X-ray mirror segments.

CONCLUSIONS

Our group focuses on all aspects related to the construction of future X-ray mirror assemblies. Our mirror segment fabrication efforts entail two separate efforts. Glass slumping will continue development to increase efficiency and reducing cost, possibly even improving figure quality. However, the next significant leap in mirror quality is expected to involve single crystal silicon mirror substrates. This technology is in its infancy stage and has yet to be proven viable.

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FABRICATION OF PARABOLIC WOLTER TYPE-I MOLDING DIE WITH 100 NM FORM ACCURACY

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1. INTRODUCTION

X-ray radiations are created in space by extremely high energy celestial events. Celestial objects such as supernova remnants, neutron stars, black holes, and clusters of galaxies can produce X-rays. However, such high energy rays cannot be reflected or refracted with conventional optics like other electro-magnetic radiations. Instead, the total reflection of X-rays over flat and smooth surfaces at very shallow angle of incidence was first reported by Compton in 1923 [1]. The discovery of this phenomenon called “grazing incidence” reflection led to the suggestion by Wolter in 1952 [2] of a number of optical configurations using confocal paraboloid and hyperboloid sections to focus X-ray radiations. The most practical is known as the Wolter type-I configuration and shown in Fig. 1.

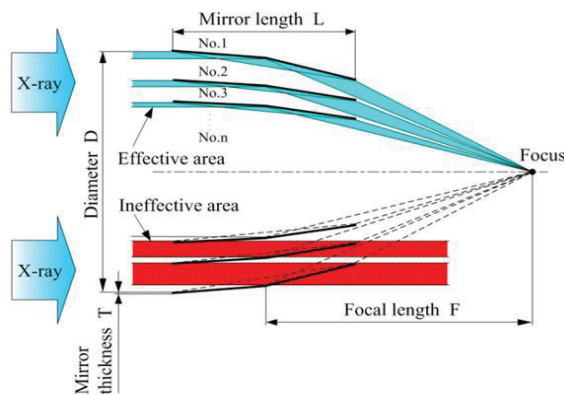


FIGURE 1. Wolter type-I configuration for grazing incidence X-ray imaging telescope.

When dealing with high energy radiations, there exists a relationship between form accuracy and surface roughness of the optical surface on one hand, and the upper limit of radiation energy that it can reflect (keV) and resolution of the images it can produce (arcs) on the other hand. State-

of-the-art finishing of molding dies has enabled the fabrication of X-ray imaging telescopes by replication, such as ASCA, XMM-Newton, Suzaku and ASTRO-H [3] shown in Fig. 2. But in future years, the goal of building high resolution aspheric hard X-ray telescopes will require stringent specification: roughness less than 0.3 nm rms and deviation from aspheric shape less than 50 nm P-V.

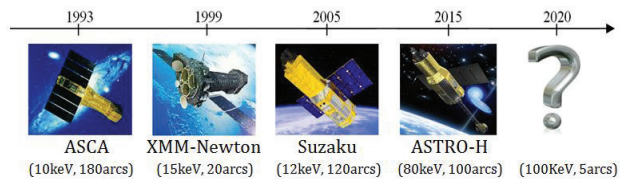


FIGURE 2. Past and future specifications of X-ray telescopes using replicated optics.

To meet this challenge, replication from optical molding dies has become the preferred method as it is economical and reliable. A few replication methods exist, amongst which replicating thin mirrors by slumping thin glass sheets (~0.2mm in thickness) across molding dies, under high temperature (see Fig. 3). This method was demonstrated in the case of the NuSTAR mission [4].



FIGURE 3. Slumping thin glass over Wolter Type-I molding die [4].

But the challenge of fabricating truly aspheric Wolter type molding dies, capable of highly accurate angular resolution (below 5 arcs), remains very expensive and time consuming. In

this paper, the fabrication of a fused silica aspheric molding die using precision grinding and corrective polishing technology is reported in details.

2. METHODOLOGY

In order to produce a demonstration parabolic molding die with focal length 8.4m, a block of fused silica was precision ground in industry to the approximate freeform shape (see Fig. 4). The shape deviation after grinding was then measured with a UA3P Ultra-Precise Coordinate Measurement Machine.

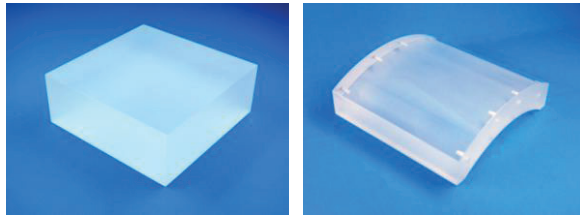


FIGURE 4. Block of fused silica, before and after grinding of the freeform shape.

A special measurement jig was made (see Fig. 5), to create a reference frame attached to the physical centre and orientation of the molding die (by intersecting lines passing through the centers of 4 silicon nitride balls).

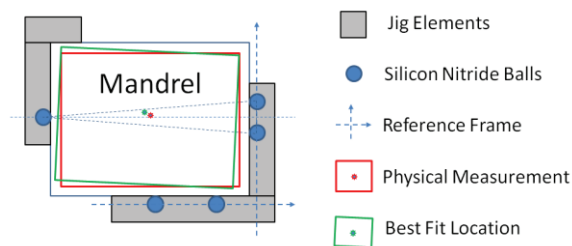


FIGURE 5. Jig referencing the best fitted error.

The centre point was then referenced against this frame using a 5th ball on the opposite side of the molding die (used to measure the exact width and length, which were then divided by 2). The jig and molding die were measured with a UA3P CMM at Panasonic in Osaka (see Fig. 6, left).

After determining the relative location of the best fitted error, this information was input into numerical optimization software that combined it with data about polishing removal rates to derive deterministic tool paths capable of reducing the form error. This tool path was run on a Zeeko 7-axis CNC machine (see Fig. 6, right).



FIGURE 6. Molding die being measured inside UA3P (left), and polished inside Zeeko CNC machine (right).

The Zeeko machine implements an innovative CNC polishing process based on tool precession which has been described in the literature [5-6] at various stages during its development. We summarize the operation of the process as follows: The position and orientation (precession angle) of a spinning, inflated, membrane-tool are actively controlled as it traverses the surface of a workpiece. The workpiece may have any general shape, including concave, flat, or convex, aspheric or free-form. While a classical polishing tool is pressurized against the surface of the part, with no attempt to control actively the Z position of the tool in a local or global coordinate frame, in the technique we describe the Z position and precession (but not directly the contact-force) are actively controlled with a CNC machine tool.

Fig. 7 shows the seven CNC-controlled axes available on the polishing machine: three axes of translation (X, Y, Z), two axes of rotation intersecting at a virtual pivot point (A, B), and two rotation spindles (H, C). It is possible to numerically control the precession angle, spindle speed, geometrical offset, and surface feed of the polishing spot to obtain variations in spot size and removal rate.

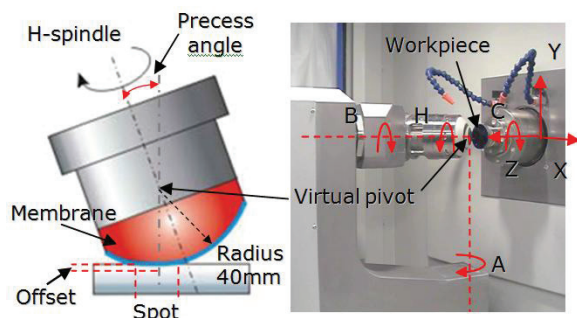


FIGURE 7. Principle of precessed polishing tool and 7-Axis CNC machine.

Additional experiments on flat fused silica substrate were also carried out using finer 0.5 μm cerium oxide abrasives, in order to estimate the best achievable finish condition on this material [6]. The results from these experiments will be used to further improve the surface finish of the actual molding die.

3. FABRICATION RESULTS

The molding die was correctively polished by iterations, using the process parameters shown in Table 1. The initial form error of the ground molding die was 36.7 μm P-V, as shown in Fig 8, left. The surface feed was moderated between 100 and 3000 mm/min in order to improve form. The result after 3 and 6 iterative corrections are also displayed in Fig. 8 (centre and right). The final improvement in 3D form error after the 6th correction was down to 155 nm P-V.

Since the most critical direction for X-ray imaging optics is along the optical axis, a series of 10 slices were also taken along the Y-direction of the final 3D error map. The resulting profiles are shown in Fig. 9. Assessment over a 150 mm section shows an overall 2D form error of 92 nm P-V, and assessment over a 100 mm sub-section shows the 2D form error down to 49nm P-V.

Simulations for a pair of mirrors replicated from molding dies with such residual form error confirm that the overall angular resolution would be below the 5 arcs target.

TABLE 1. Process parameters of corrective polishing runs.

Workpiece	Fused Silica
Polishing tool Radius Cloth	Precessed bonnet 40 mm Poromeric felt
Tool-path mode Track spacing Tool offset Head speed Precess angle Surface feed	Raster 0.5~1.5 mm 0.6 mm 2000 rpm 20 deg 100~3000 mm/min
Abrasives Grain size Density	Cerium Oxide (CeO_2) 1.5 μm 60g/L

The micro-roughness after form correction was measured using a white light interferometer at 50x resolution. After removal of 4th order terms, the 0.6 nm rms observed is adequate for slumping (see Fig. 10).

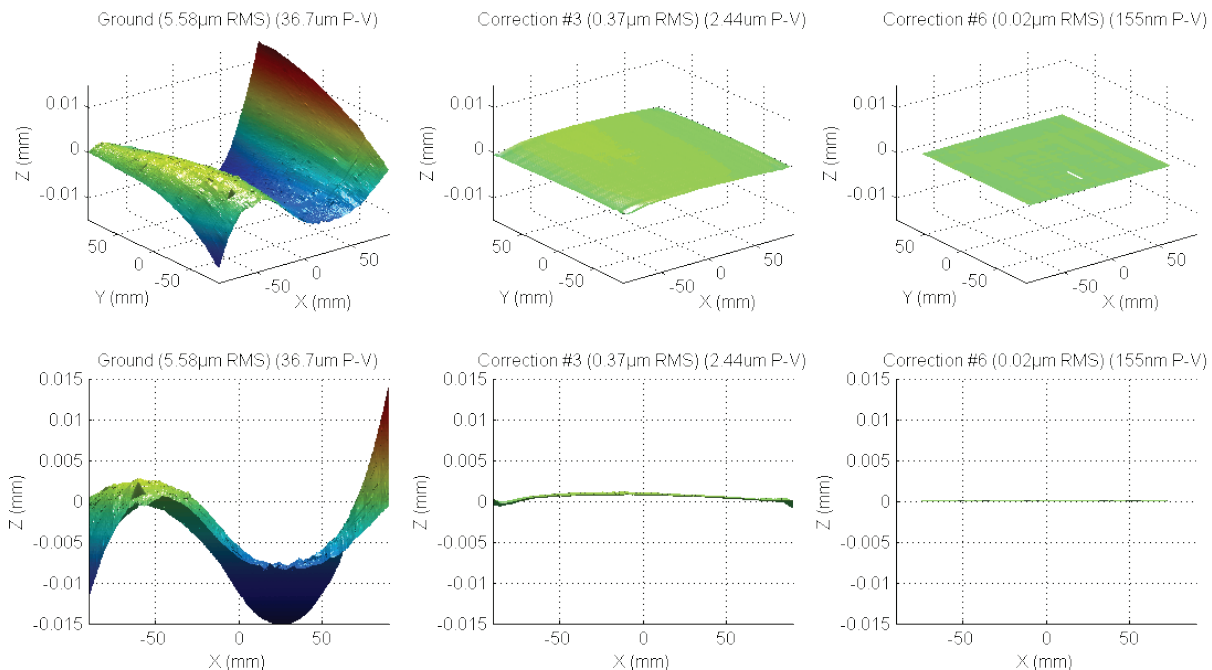


FIGURE 8. 3D form error from aspheric design, at various stages of fabrication.

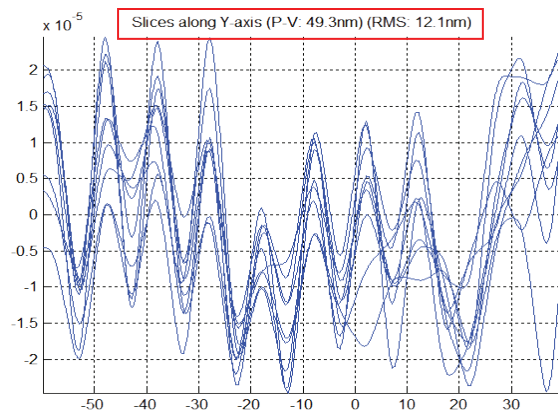
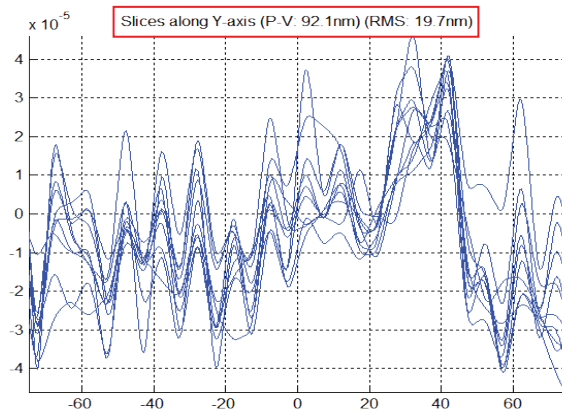


FIGURE 9. 2D form error along the optical axis, across 150 mm (left) and 100 mm (right) sections.

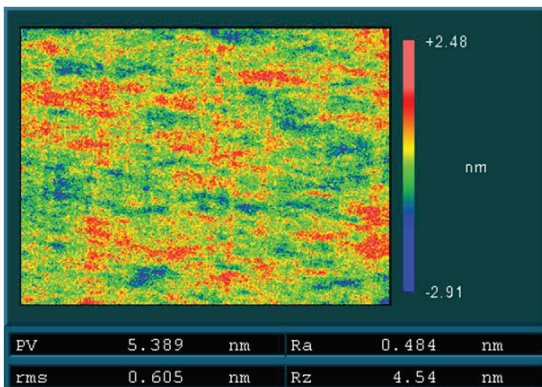


FIGURE 10. Micro-roughness (0.6nm rms).

For other replication methods such as thermal forming, which was demonstrated in the case of ASTRO-H [6], smoother micro-roughness down to 0.3 nm rms is required. A series of experiment was conducted on plano fused silica glass using finer 0.5 μm cerium oxide abrasives. The results were reported in the literature [6], and show that micro-roughness down to 0.3 nm rms (as measured by Atomic Force Microscope) is achievable on fused silica (see Fig. 11).

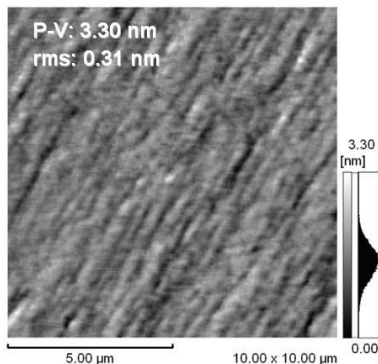


FIGURE 11. Achievable micro-roughness on fused silica using 0.5 μm CeO₂ (0.3nm rms) [6].

4. CONCLUSION

In this paper, progress on the fabrication of molding dies for replication of thin hard X-ray mirrors with angular resolution less than 5 arcs was reported. An aspheric fused silica molding die was ground and correctively polished from $\sim 36 \mu\text{m}$ down to less than 0.1 μm P-V of shape deviation along the optical axis. The intermediate surface roughness of 0.6 nm rms is adequate for replication by slumping method. The molding dies will be further smoothed down to 0.3 nm rms for application to other replication methods such as thermal forming.

ACKNOWLEDGEMENTS

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Design and Fabrication of Microinjection Molded Miniature Freeform Alvarez Lenses

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ABSTRACT

Microinjection molding is a mass production method used for affordable optical components. However, the intense nature of this process often results in part deformation and uneven refractive index distribution. These two factors limit the precision can be achieved in replicated optics. In order to understand the influences of injection molding to freeform optical devices, in this study, finite element method (FEM) was used to investigate the quality issues of the microinjection molded miniature Alvarez lenses. In addition, a metrology setup was proposed to evaluate the optical wavefront patterns in the molded lenses by using an interferometer based wavefront measurement system. This setup utilized an optical matching liquid to reduce the lens power such that the wavefront pattern with large deviation from the freeform lenses can be accommodated by a regular wavefront setup. The FEM simulation results were also used to explain the differences between the nominal and experimentally measured wavefront patterns of the injection molded Alvarez lenses.

INTRODUCTION

In a typical injection molding process, thermally induced shrinkage, non-uniform refractive index and birefringence tend to result in poor lens performance. To ameliorate these quality related issues, FEM based numerical simulation has been used to model the process and analyze its influences on optical performance of injection molded freeform optics [1-6]. In this research, we focus on how injection molding process influences freeform optics in terms of the part's deformation and refractive index variation, especially for lenses of very small dimensions.

Traditionally, the freeform optics measurement is challenging because of its non-axisymmetric nature as compared with conventional optics. If interferometers are used to measure a freeform surface with large deviation, a null lens or some type of wavefront modulator must be added to the optical setup. These techniques usually involve in expensive equipment and complex procedures. In this work, we present a simple approach to quantify freeform wavefront patterns with large deviation by reducing the lens' powers. The power reduction is realized by immersing the freeform lens in a wet cell containing an optical liquid with controlled refractive index. By comparing the collected wavefront patterns with the nominal values, the differences can be obtained and evaluated by comparing to the FEM simulation.

LENS DESIGN

The purpose of Alvarez lens is to realize variable refraction powers by a simple and compact approach. Previous studies showed that the optical refraction power of Alvarez lens pair could be easily varied by shifting two freeform surfaces perpendicular to the optical axis [7, 8]. In this paper, the bottom surface of the Alvarez lens is flat while its top surface height, in mm, can be described by the polynomial below:

$$z = a_1x^2y + a_2y^3$$

where x and y are coordinates in a circular aperture of diameter of 6 mm. In this equation, a_1 and a_2 are 0.019583063 and 0.0062879497 respectively. The deviation from the actual value of a_1/a_2 to the standard ratio of 1/3 is due to the compensation of spherical aberrations by means of the freeform surface [9]. More details of the

optical design can be found in [9]. Figure 1 shows the color map of the top freeform surface and the schematic of the mechanical design. The surface pattern is axisymmetric about y axis and negatively rotationally symmetric with respect to x axis. The maximum (peak-valley) deviation is $493.4\text{ }\mu\text{m}$.

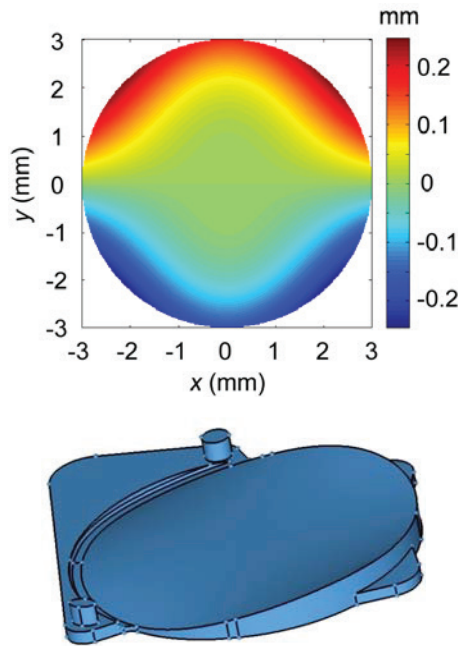


FIGURE 1. Surface variation of the Alvarez lens (top) and mechanical layout of the molded lens (bottom).

MANUFACTURING PROCESSES

The mold insert with the freeform surface was machined using ultraprecision diamond fast tool servo. The surface roughness R_a of the machined mold was approximately 6 nm . Some mechanical features were also machined into the stainless steel mold housing the mold insert as shown in Figure 1. These features were used to assemble the lens into an automatic driven system. The microinjection molding machine (LD30EH2, Sodick Plustech) used in this study is capable of injecting plastic melt at a maximum injection velocity of 250 mm/s and generating up to 30 ton maximum clamping force [5]. The injection system of this machine has a separate screw plasticizing unit and a plunger injection unit, which can precisely replicate micro features using independent controls. An optical grade polymethylmethacrylate or PMMA, code named Plexiglas V825, was used in the experiment. Figure 2 shows the molded Alvarez lens. The

conditions for molding experiments are listed in Table 1.

TABLE 1 Conditions for microinjection molding

Molding parameters	Value
Melt temperature ($^{\circ}\text{C}$)	250
Mold temperature ($^{\circ}\text{C}$)	35
Injection velocity (mm/s)	220
Maximum injection pressure (MPa)	150
Velocity/pressure switch (vol %)	89
Packing pressure (MPa)	120
Packing time (s)	3
Cooling time (s)	50
Coolant temperature ($^{\circ}\text{C}$)	25



FIGURE 2. An injection molded Alvarez lens.

FINITE ELEMENT ANALYSIS

A 3D FEM model for the Alvarez lens was first created using HyperMesh (www.altair.com) as shown in Figure 3.

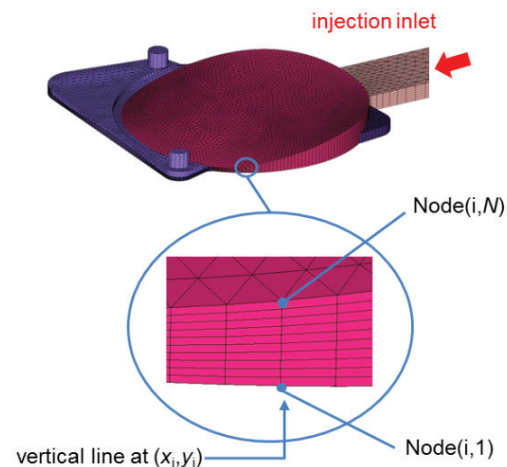


FIGURE 3. FEM model of the Alvarez lens. Node ($i, 1$) is numbered along the vertical line at coordinates (x_i, y_i) at the bottom surface, and Node (i, N) is the node along the vertical line at coordinates (x_i, y_i) at its top surface.

The FEM model was then imported into software Moldex3D (www.moldex3d.com) to perform numerical simulation. The entire lens model was meshed, based on a layer-by-layer structure [5]. There are 10 total surface layers of prism elements. Each layer can be considered a lens surface. Besides the lens itself, the runner, mold base and cooling pipes were also included in the model to ensure accuracy but were omitted from Figure 3 for clarity. The conditions for the molding simulation were the same as in the experiments listed in Table 1.

WET CELL MEASUREMENT

In an optical metrology system where interferometers are used, a freeform null lens or light modulator needs to be added, which can significantly increase the complexity of the freeform lens measurement system. Therefore, in order to reduce the optical power, such that the entire wavefront variation can be measured using the interferometer, we immersed the lens in one optical liquid with the refractive index closely match the lens' nominal refractive index [10-12]. The phase delay was reduced by a conversion factor [13]:

$$CF = \frac{n_{\text{lens}} - n_{\text{liquid}}}{n_{\text{lens}} - n_{\text{air}}}$$

where n_{lens} is the nominal surface refractive index of the lens, n_{liquid} is the refractive index of the optical liquid, and n_{air} is the refractive index of air. Reliable and repeatable measurement of wavefront aberrations using a wet cell technique was demonstrated in previous studies, for example [14]. In this study, the wet cell technique was introduced to measure the wavefront pattern of the microinjection molded freeform optics compared with conventional axisymmetric optics, and Shack-Hartmann sensor based metrology system was replaced by an interferometer in order to increase accuracy and resolution.

Figure 4 shows the interferometry based setup for wavefront measurement of the microinjection molded Alvarez lenses. The proposed setup is similar to a previously used scheme studied in [14] except in the current layout the previous double pass setup was modified to a single pass arrangement to accommodate the large deviation in freeform lens geometry. If double pass setup is adopted, the freeform characteristics of the lens, such as freeform surface power and uneven refractive index distribution will be coupled with the measured wavefront.

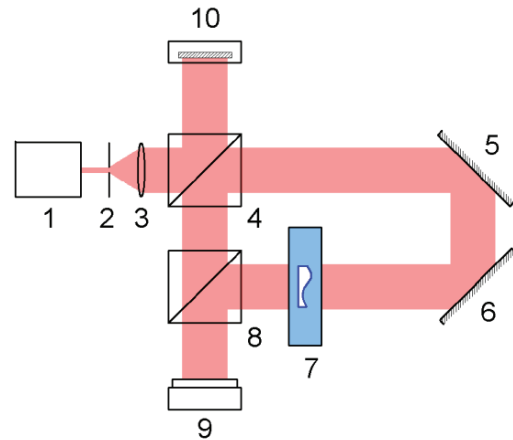


FIGURE 4 Interferometry setup for measuring wavefront pattern of microinjection molded Alvarez lens immersed in a wet cell. 1: He-Ne laser; 2: optical pin hole; 3: collimation lens; 4: beam splitter A; 5: flat mirror A; 6: flat mirror B; 7: Alvarez lens immersed in a wet cell; 8: beam splitter B; 9: CCD camera; 10: PZT stage.

RESULTS AND DISCUSSIONS

Surface deformation has its primary impact on refraction at the lens surface/air interface. The deformation in injection molding is due to the polymer's rheological properties and thermal history of the molding process. According to the FEM simulation, the surface displacements of the top freeform surface and bottom flat surface are very similar. Therefore, in order to assess the surface deformation of the Alvarez lens, only the bottom surface displacement was used as the evaluation parameter.

When the optical liquid is switched to higher refractive index of 1.5167, the wavefront pattern from the surface power can be measured. Figure 5 show the nominal wavefront pattern of an undeformed Alvarez lens with uniform refractive index, the measured wavefront pattern of the microinjection molded Alvarez lens and their difference map, respectively. The nominal wavefront deviation is 15.89λ while the measured wavefront deviation is 15.8λ , so the deviation values are close, and were normalized for comparison in Figure 5. The maximum local difference of these two wavefront patterns is less than 5%. The major differences come from the center and corner areas. The deviation trend of the wavefront differences between the nominal and the measurement is a combined result of surface deformation and refractive index variation.

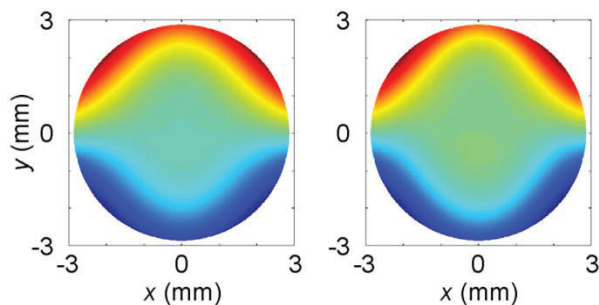


Figure 5. (a) Nominal and (b) measured wavefront pattern describing surface power distribution of the microinjection molded Alvarez lenses (color scale red -1 blue +1 mm).

CONCLUSIONS

Injection molding has many advantages over conventional glass materials for its capability of providing affordable freeform optics in high volume. This research investigates a possible process using ultraprecision diamond machining and microinjection molding to fabricate miniature Alvarez lenses. However, the geometric deformation and uneven density distribution caused by the microinjection molding process complicates its application to precision freeform optics. Therefore, we proposed an approach based on FEM simulation to visualize how both the surface deformation and refractive index variation affect the wavefront change. Moreover, a simple and fast wavefront measurement technique by using an optical matching wet cell for injection molded freeform optics was also proposed to validate the simulation.

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MICRO-RULING INTO COMPLEX SURFACE IN GERMANIUM

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INTRODUCTION

For many years, optical design has been limited to rotationally symmetric refractive optics and planar diffractive optics. Ultra-precision diamond machining has the ability to open up the optical design space to include optics with freeform and structured surfaces that can perform multiple refractive and diffractive functions simultaneously. Examples this type multiple scale optic can be found in nature. A moth's eye (Figure 1) has a large base sphere on the millimeter scale, micro-optics on the scale of tens of micrometers, and sub-micrometer (sub-wavelength) structures that act as an anti-reflective coating. For other biological eyes such as the mantis shrimp, the structured surfaces have other more complex functions such as polarization sensitivity.

Ultra-precision machining is particularly applicable for multi-scale optics in the infrared (IR) because: (1) wavelengths of operation are longer so the demands on process uncertainty are less; and (2) many IR transparent materials are brittle but amenable to diamond machining. Germanium optics in particular are difficult to manufacture in any other way. Further, ultra-precision machining offers a single platform on which freeforms can be manufactured with low positioning uncertainty, thus allowing for ruling as a secondary process.

IR/thermal optical systems typically use materials with an index of refraction of 2.5-4 and thus there is a very high reflectivity at each surface: 18-36%, respectively. They thus require a sub-wavelength anti-reflective coating for proper function. Other sub-wavelength structures can produce phenomena ranging from polarization sensitivity to complex diffractive optical functions. Further, reduction of the number of surfaces to reduce reflective

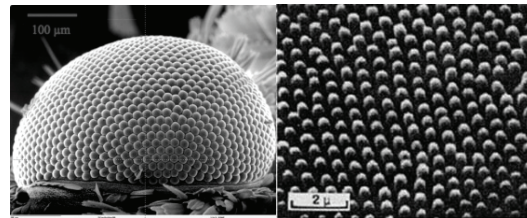


FIGURE 1: (Left) Compound moth eye ([1]). (Right) Scanning electron microscope image of fine detail on surface of moth's eye showing anti-reflective sub-wavelength surface structures ([2, 3])

losses in IR systems is desirable and thus replacing several conventional optics with one freeform optic [1] is desirable. The goal of this work is to enable the manufacture of a complex/freeform macro-lens shape in with an integrated sub-wavelength structure. In order to accomplish this, the cutting mechanics of ruling and other mechanical structuring techniques need to be understood. In this paper, we report results from non-overlapping ruling tests with dead sharp and round nose tools. Cutting and thrust forces are measured and resultant normal and friction forces and friction coefficient are reported. Tests are also conducted to determine minimum spacing of ruling lines for optical applications. The effects of crystal orientation are also investigated.

EXPERIMENTAL ARRANGEMENT

Experiments were conducted on a Moore Nanotechnology 350FG used in three axis mode (X-Z-C or X-Y-Z) as shown in Figure 2. The machine has 2000 mm/min maximum slide speed and a 10,000 RPM main spindle (C). All experiments used single crystal germanium known to be diamond turnable [1],[2],[3]. It had a (111) crystal orientation perpendicular to the top face. A Kistler 9245C dynamometer was used in all experiments. Three test configurations, shown schematically in Figure 3, were used: (a)

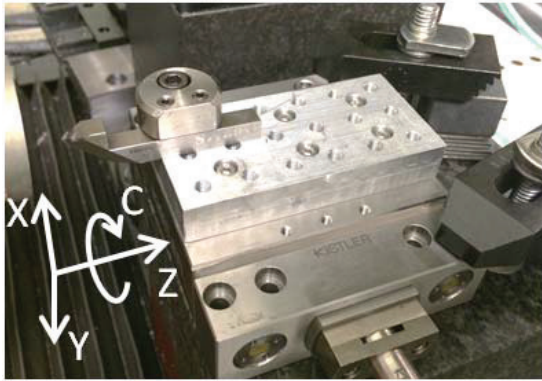


FIGURE 2. Machine and dynamometer

Ramp-in/ramp-out with round-nose tool; (b) ramp-in spiral with round-nose tool; and (c) linear grooves with a dead-sharp tool. For each arrangement the tools were single crystal diamond with a rake angle α of -45° . The round-nose tool had a radius of 0.381 mm and the dead-sharp had a radius less than 100 nm. The relief angle for the round-nose and dead-sharp were 7° and 2.5° , respectively. The included angle of the dead-sharp is 60° .

In arrangements (a) and (c), the cutting speed (V_c) is generated by the machine slides. In arrangement (b), the cutting speed is generated by coordinated C-axis motion (C-axis mode) to synchronize crystal orientation with force measurements. The dynamometer measured forces F_c and F_t at a sample rate of 1000 Hz for arrangements (a) and (b). The forces were too small to measure in arrangement (c). The cutting parameters for each arrangement are summarized in Table 1.

Table 1. Cutting Parameters

Arrangement	V_c (mm/min)	Spindle Speed (RPM)	Depth of Cut (μm)
1	100	n/a	0-2
2	n/a	10	0-2
3	100	n/a	0.05-0.2

In arrangements (a) and (b) as the depth of cut increased, the cutting becomes more fracture dominated, and this causes an increase in fluctuation of the forces and surface pitting in the workpiece. In all cases, the amount of brittle fracture depended on crystal orientation. The spiral cut allowed us to measure the changes in cutting mechanics as a function of depth and crystal orientation simultaneously.

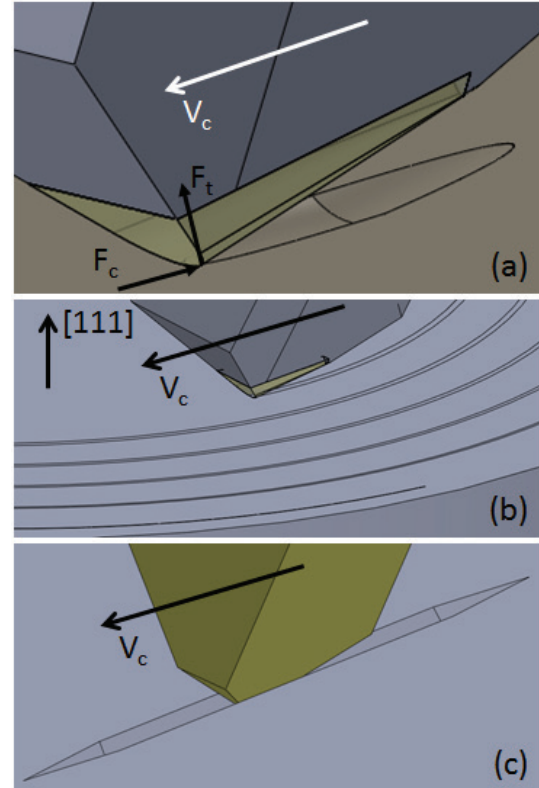


FIGURE 3: Cutting geometries (a) ramp; (b) spiral; (c) dead-sharp.

EXPERIMENTAL RESULTS

A representative micrograph of cuts made in arrangements (a) is shown in Figure 4. Forces in configurations (a) and (b) are shown in Figure 5. The data in 5(a) and (b) are both low-pass FFT-filtered at 60Hz and the data in 4(b) is additionally low-pass Gaussian filtered with a cut-off of 0.5 seconds. Figure 4 and 3(a) shows a ramp-in/ramp-out with a maximum depth 1 μm at 100 mm/min. Brittle fracture appears to dominant cutting forces at approximately 0.5 μm depth of cut. Figure 4(b) shows forces data from a spiral ramp-in with maximum depth of 2 μm at a spindle speed of 10 rpm at an increasing depth of 27.8 nm/rev. The slope of the force curve changes at approximate 0.5 μm , correlating with the ramp cut.

The ramp tests were conducted at a number of

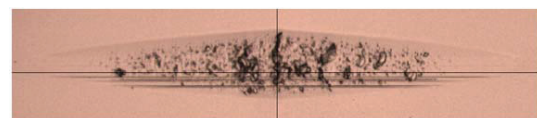


FIGURE 4. Micrograph of ramp in/out.

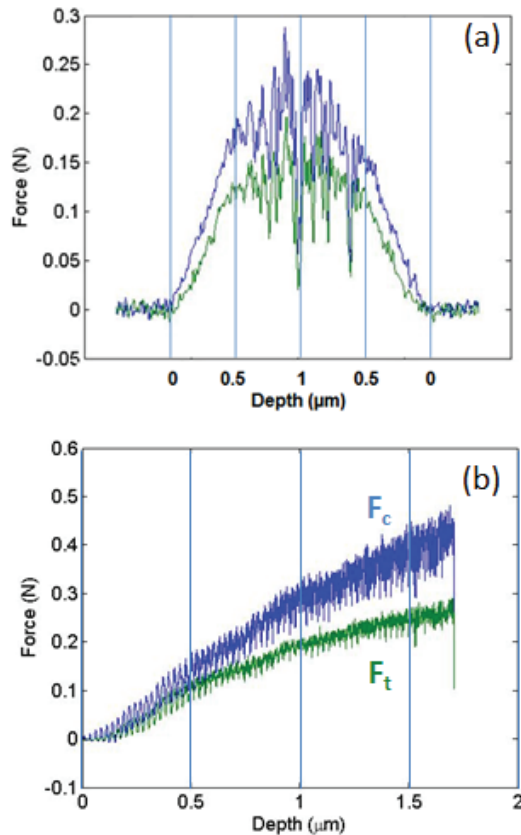


FIGURE 5. (a) Raw force signal samples.(ramp); (b) Raw force signal samples.(spiral)

depths of cut and crystal orientations. Figure 6 shows the cutting and thrust forces for 5 ramp experiments ranging in maximum depth from 0.25 μm to 1.25 μm at 100 mm/min at the center of the wafer in the same direction. To clarify the data for display, each set is low-pass Gaussian filtered at 0.075 seconds. The 0.25 μm and 0.50 μm depths appear as smooth triangles, whereas the 750 nm, 1 μm and 1.25 μm cuts all show a marked transition corresponding to the increase in brittle fracture occurring in the cutting. This further verifies the observation that increased brittle fracture appears to occur at 0.5 μm depth.

Configuration (c) is the targeted configuration for producing features with optical function. Linear grooves with spacing near the wavelength of the light passing through will produce both polarization sensitivity and an antireflective effect. Cross-cut grooves (i.e. two sets of grooves cut over one another at a 90 degree angle) will produce a strong antireflective effect.

Cutting tests show that this geometry of configuration (c) can produce well-defined

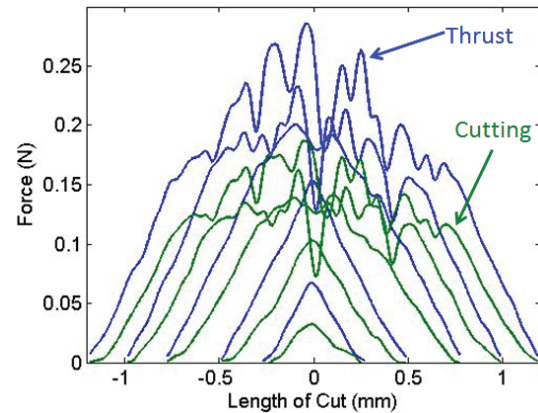


FIGURE 6. Ramp test force comparisons.

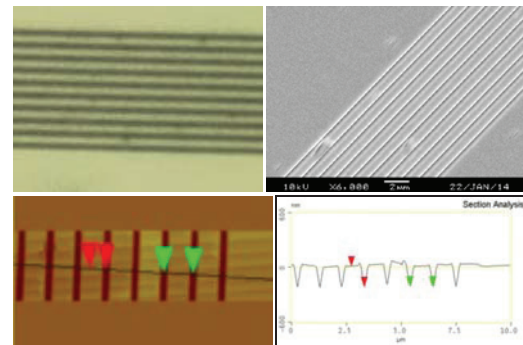


FIGURE 7. AFM data, microscope images

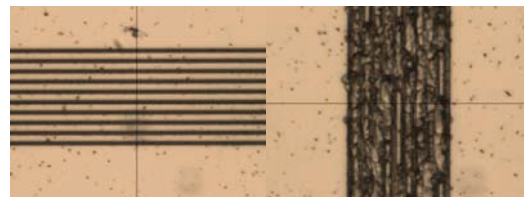


FIGURE 8: Grooves with similar parameters (3 μm spacing, 1 μm deep (left), 1.3 μm deep (right)) but orthogonal crystal orientation

grooves with little surface fracture, as shown in Figure 7. The groove depths range from 0.1-1.5 μm and the groove spacing ranges from 1-10 μm . To reduce the likelihood of fracture, grooves were cut to the final depth in several passes each below 200 nm and less than the 400 nm transition depth identified in the previous experiments. However, the direction of cut relative to the crystal orientation greatly affects the formation of fracture, as shown in Figure 8.

ANALYSIS

Force data was analyzed to identify, specific cutting energies/cutting force coefficients [5], normal and friction forces on the tool rake face and friction coefficient.

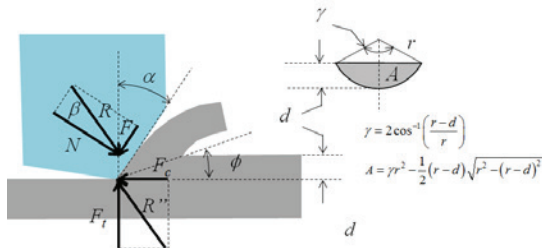


FIGURE 9. Cutting geometry and forces.

The data was analyzed with a Merchant model as shown in Figure 9. The friction force F and N are calculated using the following equations originally derived by Merchant [2].

$$\begin{aligned} F &= F_c \sin \alpha + F_t \cos \alpha \\ N &= F_c \cos \alpha - F_t \sin \alpha \\ \mu &= \frac{F}{N} \end{aligned} \quad (1)$$

Cutting force coefficients for the cutting and thrust directions were determined from Equations (2),

$$\begin{aligned} K_c &= \frac{F_c}{A} \\ K_t &= \frac{F_t}{A} \end{aligned} \quad (2)$$

where A is the cross sectional area of the uncut chip- a section of a circle as shown in Figure 9.

The six ramp experiments were divided at the center of the cut (deepest point) overlaid. The mean and standard deviations of the cutting force coefficients as a function of uncut chip area were calculated with a bucket size of 50 nm (Figure 10 (a).) Error bars represent 2 standard deviations. At low depth/area of cut, K_c has a mean value just less than 10000 N/mm² and decreases to a value less than 5000 N/mm² at higher depths of cut/area. At low depth/area of cut, K_t has a mean value more than 30000 N/mm² and decreases to a value of approximately 6000 N/mm² at higher depths of cut/area. Figure 10 (b) shows K_c versus chip area/depth for three different spiral cutting experiments. The values and trends are similar to Figure 10 (a). The decrease in cutting force coefficients with increased chip area is correlated with the increase fracture dominated cutting mechanics.

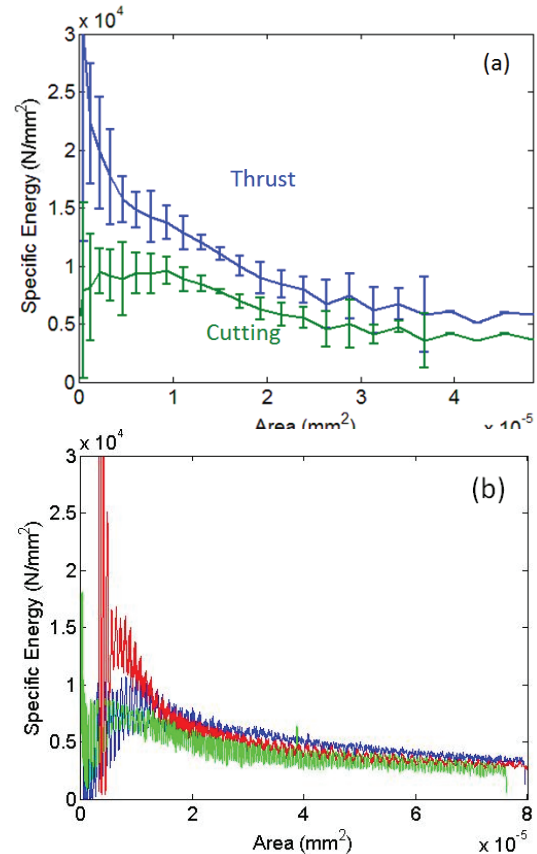


FIGURE 10. Specific Energy for the ramp-in/ramp-out (a) and the spiral ramp-in (b)

Figure 11 shows F and N as a function of chip area for the three spiral cutting experiments. In all, the normal force is significantly higher than the friction force and this correlates to a relatively low value of the friction coefficient on the rake face of approximately 0.2.

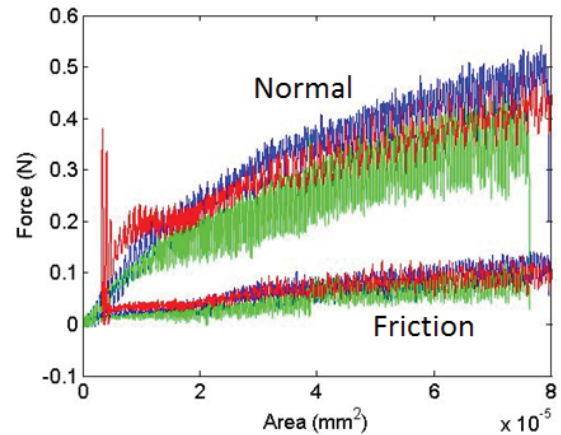


FIGURE 11. Arrangement (b), Normal and Friction Forces.

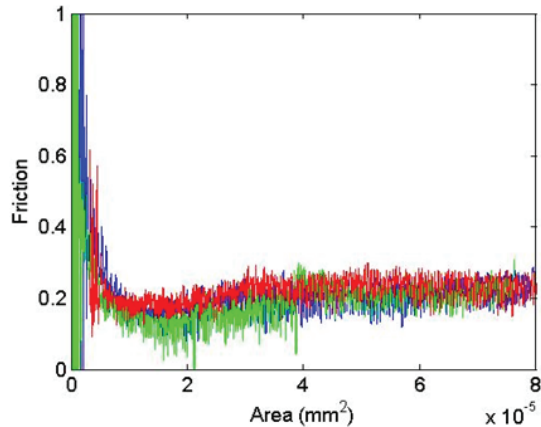


FIGURE 12. Arrangement (b) Friction versus Area.

In the spiral cutting arrangements, a three times per revolution force variation was seen to increase as the depth of cut increased. This correlated to the repetition in crystal orientations. At some orientations, more brittle chip formation occurred and led to a decrease in the average force. Figure 13, shows the increase in the three-times per revolution force variation as a function of part rotation number and hence increased depth.

DISCUSSION

The ability to structure an optical surface by ultra-precision ruling has many useful optical applications. Infrared optics are a prima application area because materials are diamond machinable and the wavelengths of operation are long, decreasing the required positioning uncertainty needed to produce a sub-wavelength structure. In this paper we have

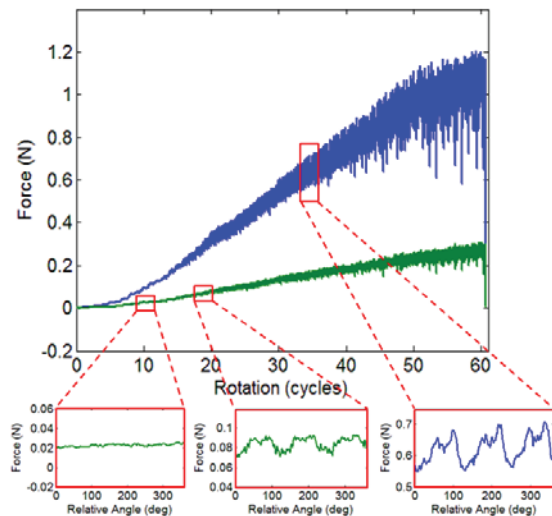


FIGURE 13. Arrangement (b), Cutting and Thrust forces showing the changes in force relative to different crystal orientations.

conducted fundamental ruling experiments in germanium to begin to identify cutting mechanics for successful ultra-precision ruling. The following are the key observations in this paper.

- Cutting forces show increased fluctuation and decreased mean amplitude as fracture begins to dominate the cutting mechanics.
- Cutting force coefficients decrease with depth of cut and the decrease correlates to fracture dominated cutting mechanics.
- Ruling behavior changes with crystal orientation for both dead-sharp and round-nosed tools.

Future work includes: (1) identifying cutting parameters necessary for robust ruling; (2) modeling of the cutting mechanics to link round-nosed cutting with dead sharp cutting; demonstration of the optical performance of micro-structured surfaces.

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METHODS AND ALGORITHMS OF DATA STITCHING AND FUSION FOR MULTI-SCALE MEASUREMENT OF ULTRA-PRECISION FREEFORM SURFACES

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INTRODUCTION

Ultra-precision freeform surfaces with submicrometer form accuracy and surface finish in nanometric range are now widely adopted in opto-mechatronics applications due to their capabilities of design freedom and special functions, as well as compact configuration for the products. However, it also brings a lot of challenges for the measurement of freeform surfaces due to the geometrical complexity and variety, especially for the large-sized and multi-scale freeform surfaces, which requires a well balance between high precision and efficiency. Currently, there is still lack of a complete solution for measurement of ultra-precision freeform surfaces by one type of measuring instrument, regarding to the multi-scale measurement, different sample materials, etc. Many existing precision measurement instruments only possess limited measuring range at one single measurement. This is particularly true for some freeform surfaces with high slope. Besides, a holistic measurement of freeform surfaces also demands the data measuring from different sensors, or multi-sensor measurement. Freeform data stitching and fusion technology not only provides an enabling solution for multi-scale and multi-sensor measurement of freeform surfaces, but also enhances the measuring ability of some high precision measurement instruments.

Data stitching and fusion is emerging technology since 1990s, and it is previously used for target tracking, automated identification of targets, and limited automated reasoning applications [1]. The technology is then adopted in reverse engineering and precision metrology [2, 3]. Generally, a procedure for data fusion includes pre-processing, data registration, data fusion and post processing, among which data registration is critical for data stitching and fusion. Due to the geometry complexity and variety, as well as a lack of common features, of freeform

surfaces, it is still very difficult to register one freeform surface to another one. Open literature review shows that, data registration can be realized by feature based or surface descriptions based approaches, and the registration process generally includes coarse and fine registration [3]. Some research work has been found for data stitching of aspherical surfaces [5, 6]. For example, subaperture stitching method is adopted to measure aspherical surfaces based on least square-fitting [7]. Iterative Closet Point (ICP) method is often used for coordinates correspondence searching. However, they are susceptible to data noise and outliers involved in the measured data. There is still little research found for data stitching and fusion of ultra-precision freeform surfaces with sub-micrometer form.

As a result, this paper presents methods and algorithms for data stitching and fusion for measuring ultra-precision freeform surfaces based on Intrinsic Surface Features (ISFs). The stitching process will be explained. Some important algorithms will be explained, including the data format unification, calculation of ISFs such as Gaussian curvatures and mean curvatures, registration of ISFs map, data processing for the overlapping area, etc. Preliminary experimental studies will be presented and the results are discussed.

ALGORITHM OF DATA STITCHING AND FUSION BASED ON INTRINSIC SURFCAE FEATURES

Intrinsic surface features (ISFs) refer to those surface features whose values are invariant under the transformation (rotation/translation) of the embedded coordinate frame and also free from the implicit parameterization of the surface. Surface stitching can be performed without the need to consider the misalignment of the coordinate frames and the parameterization of the surfaces when the two surfaces are

represented by ISFs. Two of the important ISFs, are Gaussian and mean curvature. They uniquely determine the surface shape according to the Gaussian Curvature Uniqueness Theorem and the Mean Curvature Uniqueness Theorem [8, 9].

Figure 1 shows a flow chart of the data stitching and fusion. Different sets of data are obtained with measurement setup information, and/or, surface normal /CAD model is provided. The data format will be unified, and hence the ISFs of the data sets are calculated, followed by the registration to find the correspondence. After that the data sets are fused by the overlapping data processed such as data trimming. When the normal surface or CAD model information is provided, different sets of data are registered to the normal surface or CAD surface firstly, and hence they are stitched to each other. Figure 2 illustrates data stitching process with the aid of normal surface/CAD model. If there is no CAD information, different sets of data are directly registered to each other by maximizing the similarity of their overlapping area. Some important algorithms in the stitching process are explained in details in the coming sections.

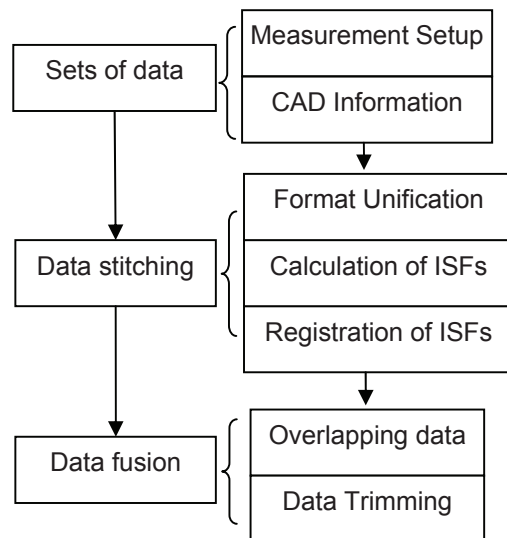


FIGURE 1. Flow chart of data stitching and fusion.

Data Reformat by Re-sampling

Before the registration to the normal surface by ISFs, the format of different data set is unified by a re-sampling strategy [10]. This is realized by the process as shown in Figure 3. Firstly, one point and two perpendicular vectors on the surface of data sets and normal surface are

defined. The grids of points with certain spacing in the two perpendicular vector directions based on Woven mesh model are then generated [11]. Since the even mesh fitted into a freeform surface leads to the strain energy, the distribution of sampled points is optimized by minimizing the strain energy. After that, the format of data sets and normal surface are unified for the later registration based on ISFs.

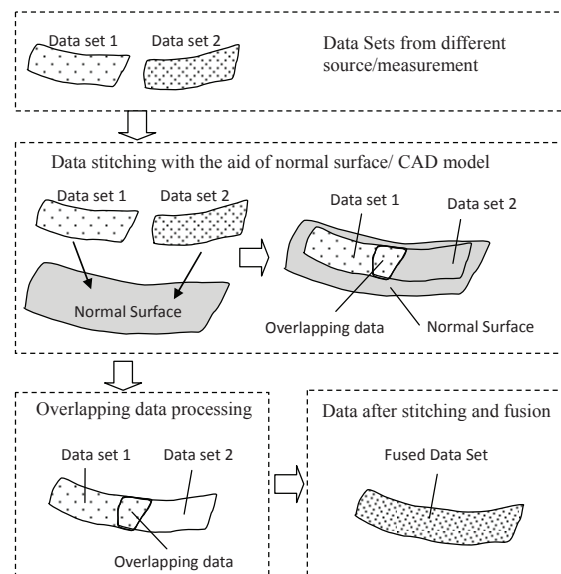


FIGURE 2. Graphical illustration of data stitching and fusion with the aid of normal surface/CAD model.

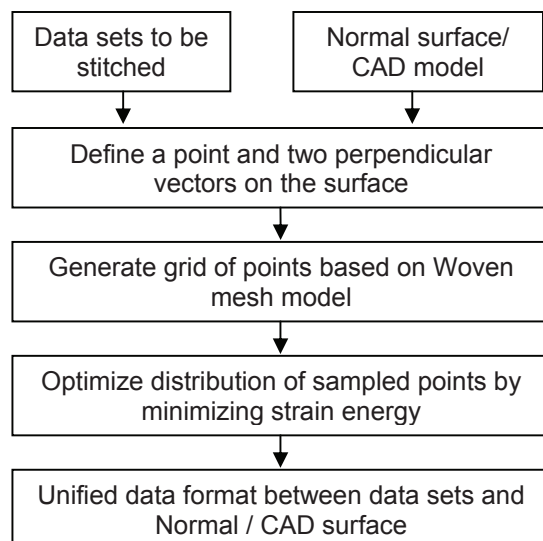


FIGURE 3. Flow chart for data format unification.

Calculation of Intrinsic Surface Features

In the present research, Gaussian curvature or mean curvature is chosen as the Intrinsic Surface Features (ISFs). After the data format unification as discussed in previous section, the calculation of Gaussian and means curvature of a freeform surface ($S(u, v)$) is presented as follows.

The first fundamental form of a surface $S(u, v)$ can be expressed as

$$I = Edu^2 + 2Fdudv + Gdv^2 \quad (1)$$

Where,

$$\begin{cases} E = S_u^2 = \left(\frac{\partial S(u, v)}{\partial u} \right)^2 \\ F = S_u S_v = \frac{\partial S(u, v)}{\partial u} \frac{\partial S(u, v)}{\partial v} \\ G = S_v^2 = \left(\frac{\partial S(u, v)}{\partial v} \right)^2 \end{cases} \quad (2)$$

The second fundamental form of $S(u, v)$ is given by

$$II = Ldu^2 + 2Mdudv + Ndv^2 \quad (3)$$

Where,

$$\begin{cases} L = \frac{\partial^2 S(u, v)}{\partial u^2} \cdot \frac{S_u \times S_v}{\|S_u \times S_v\|} \\ M = \frac{\partial^2 S(u, v)}{\partial u \partial v} \cdot \frac{S_u \times S_v}{\|S_u \times S_v\|} \\ N = \frac{\partial^2 S(u, v)}{\partial v^2} \cdot \frac{S_u \times S_v}{\|S_u \times S_v\|} \end{cases} \quad (4)$$

Then, Gaussian curvature K and mean curvature H of the surface $S(u, v)$ are determined by the coefficients of the first and second fundamental forms as follows, respectively.

$$K = \det \begin{bmatrix} E & F \\ F & G \end{bmatrix}^{-1} \det \begin{bmatrix} L & M \\ M & N \end{bmatrix} \quad (5)$$

$$H = \frac{1}{2} \text{tr} \left(\begin{bmatrix} E & F \\ F & G \end{bmatrix}^{-1} \begin{bmatrix} L & M \\ M & N \end{bmatrix} \right) \quad (6)$$

Where, operator $\det(\bullet)$ is the determinant of a matrix, operator $\text{tr}(\bullet)$ is the trace of a matrix.

Registration Process based on ISFs

The surface stitching problem is now converted to ISFs registration. That is, the corresponding searching in 3D Cartesian coordinate frame by solving six parameters (3 translations+3 rotations) is now performed in 2D space by finding 3 parameters (2 translations+1 rotation), as shown in Figure 4. Registration problems involving translation and rotation are recovered by applying Fourier-Mellin transform and phase correlation method [12].

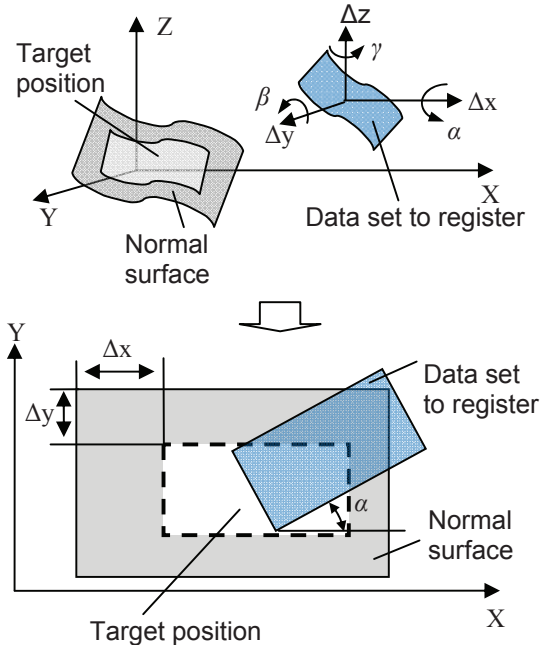


FIGURE 4. Illustration of data registration based on ISFs.

Suppose $f_2(x, y)$ is translated and rotated from $f_1(x, y)$, then

$$f_2(x, y) = f_1(x \cos(\alpha) - y \sin(\alpha) - \Delta x, x \sin(\alpha) + y \cos(\alpha) - \Delta y) \quad (7)$$

Where, $(\Delta x, \Delta y)$ is translation distance and α is the rotation angle. According to the Fourier translation property and the Fourier rotation property, the Fourier transformation of f_1 and f_2 are related by

$$\begin{aligned} F_2(\xi, \eta) &= \exp(-j2\pi(\xi\Delta x + \eta\Delta y)) \cdot \\ F_1(\xi \cos(\alpha) - \eta \sin(\alpha), \xi \sin(\alpha) + \eta \cos(\alpha)) \end{aligned} \quad (8)$$

Where, F_1 and F_2 are Fourier transform of f_1 and f_2 , respectively. Therefore, Eq. (7) and Eq. (8) preserve

$$\begin{aligned} M_2(\xi, \eta) = \\ M_1(\xi \cos(\alpha) + \eta \sin(\alpha), -\xi \sin(\alpha) + \eta \cos(\alpha)) \end{aligned} \quad (9)$$

Where, M_1 and M_2 are magnitude of F_1 and F_2 , respectively.

From Eq. (9), the translation is recovered, and the rotation causes the spectral magnitude to be rotated with the same angle, which can be further determined using the phase correlation technique by representing the spectral magnitude in polar coordinates [13], as shown in Eq. (10).

$$PM_2(\rho, \theta) = PM_1(\rho, \theta - \alpha) \quad (10)$$

Where, PM_1 and PM_2 are the spectral magnitude of f_1 and f_2 in polar coordinates, respectively.

The translation offsets are determined by using the phase correlation again after rotating f_2 , which is realized by extracting and correlating the phase of both PM_1 and PM_2 as follows

$$\begin{aligned} Corr(u, v) &= \frac{FPM_1(u, v)}{|FPM_1(u, v)|} \cdot \frac{FPM_2(u, v)}{|FPM_2(u, v)|} \\ &= \exp(-2\pi(u + v\alpha)) \end{aligned} \quad (11)$$

Where, FPM_1 and FPM_2 are the Fourier transform of PM_1 and PM_2 .

The Inverse Fourier Transform (IFT) of Eq. (11) is a Dirac δ -function yielding a sharp maximum at $(0, \alpha)$. Hence, the f_2 is rotated by α (or $\alpha + \pi$), and the rotated f_2 is phase correlated with f_1 . The maximum value of the outputs is located and is considered as the translational offset $(\Delta x, \Delta y)$ of f_2 .

Figure 5 summarizes the algorithms to find out the translation and rotation offsets between the two ISFs maps. When two feature maps (f_1, f_2) are input, 2D Fast Fourier Transformation (FFT) is undertaken and then the spectral magnitude of the two feature maps (PM_1, PM_2) are

obtained, which is then transformed to polar coordinate system (PR_1, PR_2) . The spectral magnitude in polar coordinate representation is then processed by 2D Inverse FFT (IFFT), and then the rotation angle (α) is obtained by phase correlation operation. The ISFs map to be registered is rotated with the angle (α) , then phase correlation is employed again between the two ISFs maps to find the translation offset $(\Delta x, \Delta y)$. Hence the two ISFs maps are registered with the correspondence established by the offsets (rotation and translation).

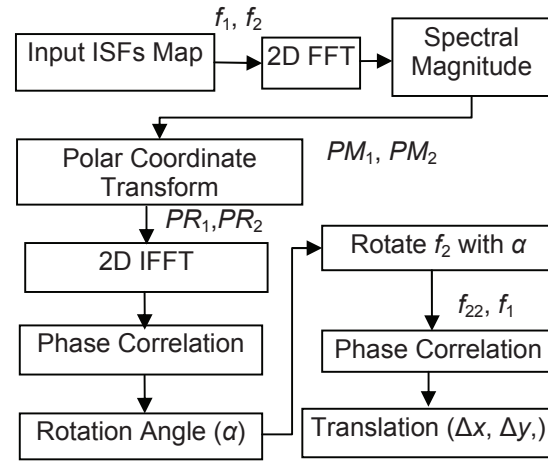


FIGURE 5. Flowchart of ISFs map registration process.

Data Processing for Overlapping Area

After data registration, the overlapped data between the two data sets are processed so as to form one fused data set. There exist challenges for overlapped data fusion when the data sets are from different sources (e.g. different sensors) due to the variety of data density and uncertainty involved. In the current study, data trimming is taken to process the data in the overlapping area. For example, the data with high precision/small uncertainty trim the data set from low precision/large uncertainty. That is, high precision data is retained while low precision data is omitted in the overlapping area. Figure 6 illustrates the processing of data in overlapping area. As shown in Figure 6, if the precision of data set S_1 is higher than that of S_2 , then S_2 is trimmed by S_1 . If the data in overlapping area is S_0 , then the final fused data set is

$$S = (S_1 + (S_2 - S_0)) \quad (12)$$

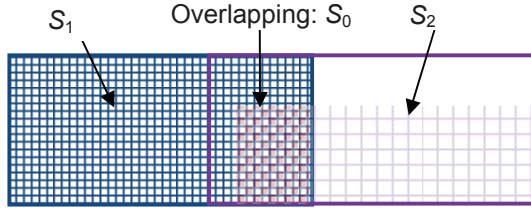


FIGURE 6. Illustration for overlapping data processing after registration.

PRELIMINARY EXPERIMENTAL STUDIES

Experiment Design

To study the effectiveness of the proposed method, preliminary experimental studies were conducted to stitch data sets without the aid of normal surface / CAD model. A freeform surface is designed as follows:

$$\begin{cases} f(x, y) = \sin(0.5x) + \cos(0.5y) \\ x, y \in [-2\pi, 2\pi] \end{cases} \quad (13)$$

A portion of the designed surface with dimension $x, y \in [-\pi, \pi]$ is extracted and is served as one of the stitching surfaces, denoted as S-A. Another five portions, i.e. the S-B1, S-B2... S-B5, are extracted on the design surface with the same dimensions. All of five portions have certain overlap with the S-A respectively. Hence, five pairs of surfaces are to be stitched. The relative position among these surfaces is summarized in Table 1. tx and ty are the offsets (in mm) of centers of the two stitching surfaces; ROL is the rate of the overlapping between two stitching surfaces. In the experiment, the S-B1 to S-B5 are moved to a position to indicate the coordinate frame misalignment between the two stitching surfaces. Hence the proposed ISFs registration based method is used to perform surface stitching of two surfaces which are not in the same coordinate systems. Figure 7 shows the ISFs of the S-A, S-B1 to S-B5.

TABLE 1. Relative position between five pairs of stitching surfaces.

Variables	S-B1	S-B2	S-B3	S-B4	S-B5
tx (mm)	3	6	0	0	3
ty (mm)	0	0	3	6	3
ROL	75%	50%	75%	50%	50%

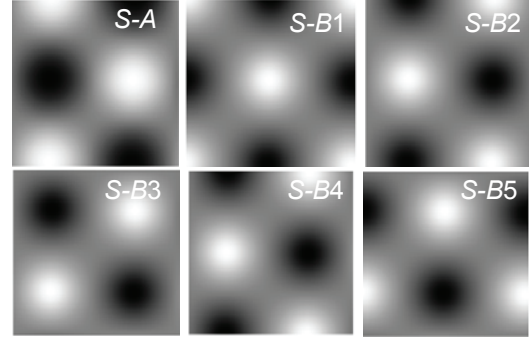


FIGURE 7. ISFs of surface S-A, S-B1 to S-B5.

Results and Discussions

Figure 8 shows one case of ISFs registration results between the S-A and S-B5. The accuracy of the stitching results is characterized by the error of the estimated relative position between the pair of the stitching surfaces (ε_{tx} , ε_{ty}). Table 2 summarizes the stitching results. It is noted from the results that stitching error is decreased with the increasing of the rate of the overlapping of the two surfaces. The results also verify that the proposed method is able to achieve nanometer level of stitching accuracy when the overlapping of the two surfaces is more than 50%. In the further study, data stitching and fusion considering the uncertainty propagation, data meshing method for the final fused data set, etc will be investigated.

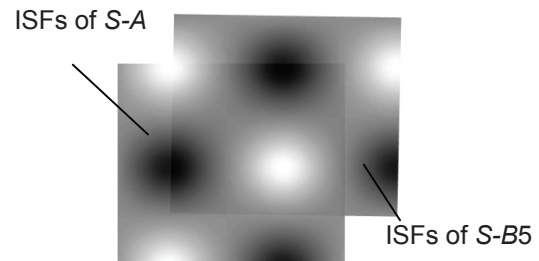


FIGURE 8. ISFs registration results between S-A and S-B5.

TABLE 2. Errors of estimated relative position between the stitching surfaces.

Error	S-B1	S-B2	S-B3	S-B4	S-B5
ε_{tx} (nm)	21.2	12.1	-11.7	23.5	2.3
ε_{ty} (nm)	-10.9	35.6	-7.1	47.2	52.6

CONCLUSION

Freeform data stitching and fusion technology provides an enabling solution for multi-scale and multi-sensor measurement of freeform surfaces, and also for enhancing the measuring ability of some high precision measurement instruments. This paper presents methods and algorithms for data stitching and fusion for measuring ultra-precision freeform surfaces based on registration of Intrinsic Surface Features (ISFs), with and without normal surface or CAD model. Some important algorithms involved in the stitching algorithms are explained, including the data format unification, calculation of ISFs such as Gaussian curvatures and mean curvatures, registration of ISFs map, data processing for the overlapping area, etc. Preliminary experimental studies have been undertaken and the results are discussed. The proposed method and algorithms of data stitching and fusion intend to eliminate or alleviate the effect of noise and outliers, and provide a robust stitching method, which is helpful for multi-scale and multi-sensor measurement of ultra-precision freeform surfaces.

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FUSING DATA FROM MULTIPLE INTERFEROMETERS FOR THE MEASUREMENT OF OFF-AXIS ASPHERES

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INTRODUCTION

In this paper we summarize a method that we have implemented at the National Facility for Ultra Precision Surfaces in North Wales, for the production of prototype primary mirror segments for the European Extremely Large Telescope. We focus on the stitching of sub-aperture data patches from a sub-aperture interferometer, onto a full-aperture phase-map, where several small areas of the full-aperture data set are missing owing to obscurations, reflections and ghosts from the full-aperture test.

Stitching from interferometry has been around for several years as a means to allow the measurement of larger parts than would otherwise be possible [1]. The most commonly used technique involves making a series of overlapping measurements of a surface using Fizeau interferometry, and using the overlap areas to stitch the data together using piston, tilt, power and other terms as required by the particular geometry [2]. Working with stitching on more complex aspheres requires secondary metrology and/or custom null correctors to provide appropriate aspheric wavefronts and reference datum information.

We now look at early steps made to fuse data from two separate interferometers where the data from the second interferometer are stitched onto a data set from the first, allowing the data from the first interferometer to form the basis 'reference' data set. There is therefore a requirement that there is significant overlap between the data sets.

The prototype segments in production at the National Facility for Ultra Precision Surfaces are particularly challenging as they have a long 84 m radius of curvature and are off-axis aspheres, thus requiring complicated full-aperture interferometric testing. In this paper we describe how we have combined the full-aperture interferometry data with sub-aperture data to provide a specific lateral resolution over an entire segment, even in the presence of obscurations in the full-aperture test.

FULL-APERTURE TEST

The full-aperture test of the segments involves shortening the 84 m conjugate to fit inside a building and making the optical path length more feasible. A vertical test tower is used with the part mounted on the worktable of the polishing machine, the interferometer mounted on a mezzanine on the tower, and a folding sphere mounted high-up on the test tower. The test is at null for each segment. Figure 1 shows a picture of the test tower for reference.

Light exits horizontally from a dynamic phase-shifting interferometer and passes through an adjustable optical null correction system before being folded vertically upwards by a small fold mirror located a few metres directly above the optical surface under test. Light from this mirror is reflected upwards to a large spherical mirror approximately 1.4 m in diameter. This large mirror provides most of the optical power to shorten the total optical path length of the test. When the light from this mirror reaches the surface under test, located on the polishing machine work-piece table, the rays strike the surface at near-normal incidence and retrace

back through the optical system to form the interference pattern with the reference inside the Twyman-Green full-aperture interferometer.



FIGURE 1. Picture of the full-aperture test tower showing a mirror segment mounted on the IRP polishing machine. The top fold mirror is out-of-shot at the top of the tower.

The presence of a fold mirror above the surface under test means that part of the surface is not visible in the full-aperture test. Furthermore, a CGH providing a few waves of residual correction close to the interferometer ghosts several artefacts and reflections in the optical system over some small portions of the full-aperture test. It is for these reasons that a stitching solution was first proposed such that a complete full-aperture map could be provided to satisfy the technical requirements of the ESO segment specification [3].

Parts of the full-aperture optical test train are calibrated by the use of a master spherical segment and other custom optical components that are inserted into the optical test at the time of calibration. The net result of the calibration is a full-aperture measurement that reveals the shape deviation of the segment under test from the ideal segment shape.

STITCHING OFF-AXIS ASPHERES

Stitching interferometry on aspheres usually requires some form of null-corrector element and/or some external metrology to fully capture the parameters of the surface under test. These parameters include the conic constant, the base-radius and the exact quantities of the low-order Zernike terms that actually make up the surface under test as opposed to coming from

misalignments between the interferometer and test piece.

In this work we have avoided the requirement for extra metrology and null correctors for the stitching interferometer by using the calibrated full-aperture map as our reference.

In this work the areas on a segment requiring sub-aperture measurements are:

1. Edge and corner data (where necessary).
2. Central obstruction (folding flat).
3. CGH artefacts and strays.

This leaves clear requirements for the stitching software:

1. The software must be able to match terms of higher order than piston, tilt and power in order to stitch a sub-aperture patch onto a full-aperture data set.
2. There must be some mechanism to provide data registration in the lateral domain between the sub- and full-aperture data.
3. There must be some method to establish a common data grid between the full-and sub-aperture data sets.
4. There must be sufficient overlap between sub- and full-aperture data in order for the stitching to reliably match the low-order surface terms of the data sets.

Sub-aperture Hardware

Our hardware solution relies on the use of a 4D Technology PhaseCam 6000 Twyman-Green interferometer mounted to a custom beam expander providing a nominally collimated output beam of 183 mm diameter. The interferometer head unit is used with an f/4 test lens feeding into the finite conjugate of the beam expander's 800 mm BFL conic aspheric collimating objective lens.

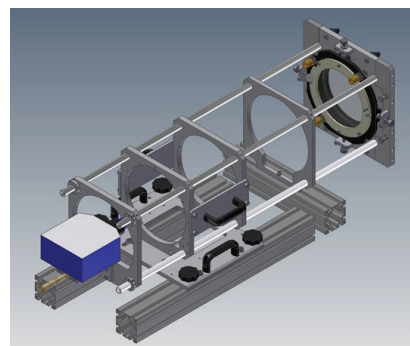


FIGURE 2. Schematic of the sub-aperture interferometer. The interferometer is at the left,

and the aspheric collimating lens is at the far right of the truss along with the screws for adjusting the output wavefront.

The whole beam expander assembly, as shown in figure 2, is mounted on a truss-tube assembly and is designed to be mounted vertically to the polishing head of a Zeeko IRP 1600 polishing machine. The exact position of the collimating objective lens is adjustable along the optical axis by means of a micrometer screws such that the power term, and other rotationally symmetric Zernike terms, in the output wavefront can be varied within a predefined range. This adjustment is important for matching the incident wavefront to the bulk shape of the segment under test – the defocus term being predominant. By taking the beam-expander away from normal adjustment we are able to produce a quite spherical output beam with the required 84 m radius of curvature that best matches the local segment under test by setting off against an 84 m radius calibration sphere. With this approach, a sub-aperture test is not null, and the interferogram is dominated by power, astigmatism, coma and spherical aberration terms. However, the net departure is of the order of 3 – 5 μm depending on the actual segment's design and its state of figure accuracy – see for example figure 3.

The sub-aperture system can cope with approximately 240 μrad of slope error before the onset of phase-unwrapping problems.

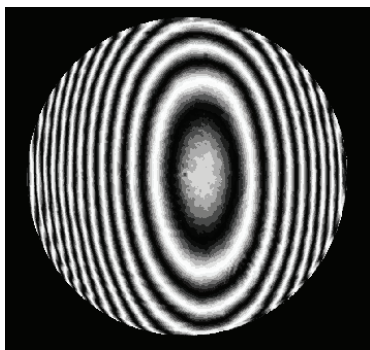


FIGURE 3. Typical sub-aperture interferogram from the system containing tilt, power, astigmatism and coma terms. This interferogram shows 6.8 μm of wavefront form.

The idea of the test is that stitching processor will modify a selectable number of lower order terms to stitch this patch onto the calibrated full-aperture data once lateral registration

information has been provided and the data sets put onto a common grid.

LATERAL ALIGNMENT

The question arises of how to locate the field of view of the sub-aperture interferometer relative to the full-aperture data set. In this work we fiducialize the surface under test after we have assembled the final full-aperture phasemap, and then take a full-aperture image of the fiducialized surface. The fiducials are also arranged to be visible in each sub-aperture data set where two measurements are made:

1. An intensity measurement with the fiducials visible in the sub-aperture phase data.
2. A phase measurement with the fiducials removed where the part-interferometer relationship was untouched from (1) above.

The basic process flow is as follows:

1. Make the full-aperture measurement using the optical test tower.
2. Place removable fiducials onto the surface of the full-aperture test at pre-defined positions such that they will be visible in the required sub-aperture tests.
3. Make a full-aperture intensity measurement showing the positions of the fiducials.
4. Locate the sub-aperture interferometer onto the polishing machine z-axis.
5. Move the sub-aperture interferometer over to the next sub-aperture and locate the best 'null'.
6. Make an intensity measurement and save the result.
7. Remove the fiducials for the current sub-aperture without moving the interferometer-part relationship.
8. Make a full-field sub-aperture phase measurement and save the result.
9. Repeat from 4 until all the sub-apertures have been measured.

SOFTWARE

The Stitching Software used in this work is part of Zeeko Ltd's Stitching Toolkit and forms a custom module for stitching data from independent interferometers where the lateral sub-aperture registration is found by fiducial information. If necessary, pre-determined transformations can be applied.

In general, for each sub-aperture we require a rotation of the sub-aperture phasemap about the data z-axis, an X-Y translation, and a scale factor (possibly separate X- and Y-scale factors) to place it in the correct location and in scale relative to the full-aperture data. A minimum of 3 non-collinear fiducials per sub-aperture is required to achieve this, and we use at least 4 fiducials per sub-aperture and solve for the rotation, translation and scale from interferometer pixel coordinates to full-aperture data coordinates in a least-squares sense. This lateral registration is the action of the first stage of the stitching processor.

Once the transformations from sub-aperture to full-aperture coordinates are known the sub-aperture data can be re-interpolated onto the same basis grid of points as the full-aperture data set. We transform the (x, y, z) points to the new locations and re-interpolate z onto the full-aperture (x, y) measurement locations.

TABLE 1. Basic data properties of the full- and sub-aperture data.

Test	Lateral mm/pix	Array Size
Full-aperture	0.84	1968 x 1947
Sub-aperture	0.26	996 x 1004

At present, the requirement is to sample the surface at 1 mm/pixel, and this forms the basis for down-sampling the sub-aperture data onto the 0.84 mm pixel grid of the full-aperture test. It also ensures that the full-aperture test is not modified in any way by the stitching algorithm. With the current software it is also theoretically possible to measure the entire surface using sub-apertures and use the full-aperture reference to estimate the low-order shape to result in a data map at the resolution of the sub-aperture interferometer. Desktop computer memory requirements are the effective limit to using this technique at present.

Full-aperture Fiducials

A full-aperture measurement is loaded into the software and the user can point to a fiducial on screen and assign a number to it. A similar process can be performed for a sub-aperture fiducial. Fiducials can then be associated with an actual measurement phasemap. An example of a full-aperture intensity measurement with fiducials is shown in figure 4.

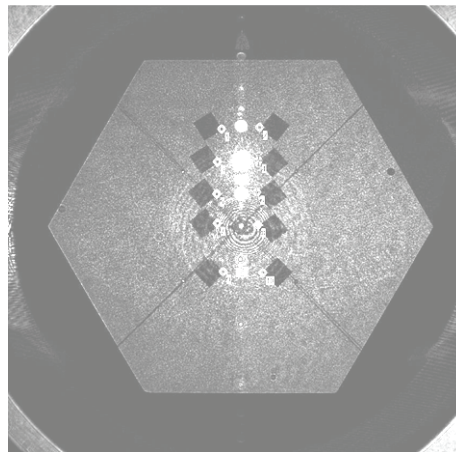


FIGURE 4. Intensity image for from the full-aperture test showing 10 fiducials placed around the obscured areas of the test.

Sub-aperture Fiducials

Sub-aperture fiducials are found in the same way as for full-aperture fiducials and it is incumbent upon the user to apply the correct correspondances to avoid errors. In practice this is easily achieved with a crib-sheet containing a drawing of the fiducials. After the user points to a fiducial on the image, figure 5, it is assigned a number and its coordinates in the sub-aperture frame appear on a spreadsheet as in figure 6.

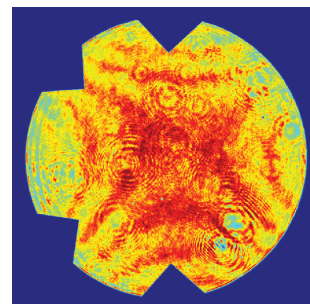


FIGURE 5. Typical sub-aperture intensity image. The points of the obscured areas are used as the fiducial coordinates in both the full- and sub-aperture tests. The user simply places a fiducial in software at the corner of each triangle.

	Full-X	Full-Y	Sub-Fids	Sub-X	Sub-Y	St...	Sub-File
1	954.77	1521.27	Fids 1-4.h5	270.81	363.14	☒	Sub Ap fids 1-4_cal_low term...
2	1053.54	1513.35	Fids 1-4.h5	286.52	688.97	☒	Sub Ap fids 1-4_cal_low term...
3	917.50	1450.45	Fids 1-4.h5	502.43	255.18	☒	Sub Ap fids 1-4_cal_low term...
4	1083.82	1450.45	Fids 1-4.h5	492.61	779.26	☒	Sub Ap fids 1-4_cal_low term...

FIGURE 6. Fiducial table showing the measured coordinates of the fiducials in the full- and sub-

aperture data files. Each row corresponds to a single fiducial and coordinates are in pixel units.

Registration

After the sub-aperture registration transformations have been derived the coordinates of the full- and sub-aperture fiducials are shown in the spreadsheet for analysis if required as exemplified in figure 7.

Transformed Fiducials							
	Full-X	Full-Y	Sub-X	Sub-Y	rX	rY	rMag
1	954.77	1521.27	953.72	1521.45	1.05	-0.19	1.07
2	1053.54	1513.35	1055.23	1514.50	-1.69	-1.16	2.05
3	917.50	1450.45	918.59	1449.90	-1.09	0.55	1.22
4	1083.82	1450.45	1082.09	1449.66	1.73	0.79	1.90

FIGURE 7. Transformed fiducials example. Here, the mean residual between full- and sub-aperture fiducials after transformation is approx. 1.56 pix = 1.31 mm in the full-aperture test.

Once registration is complete the processor then determines the overlap function – how many measurement data sets contribute at a given x,y coordinate. A typical example of an overlap function is shown in figure 8 where the hexagonal segment is clearly shown along with 2 sub-aperture locations.

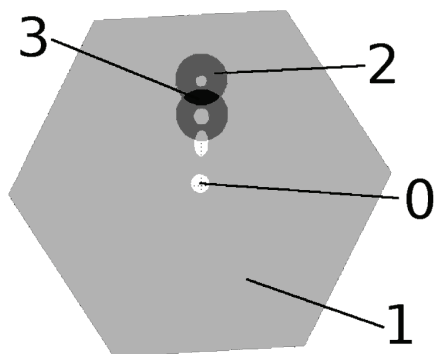


FIGURE 8. Example overlap function showing the number of phase measurement values at each x-y location.

STITCHING

The stitching algorithm used in this work keeps the full-aperture data as the reference set. Therefore no operations are performed on the full-aperture data, other than using it to derive stitching transformations for each sub-aperture data set. A subtraction of the input full-aperture data set and the output stitched data set reveals

zero everywhere except for where the sub-aperture data overlaps the full-aperture test.

The stitching software computes a set of Zernike polynomials for each sub-aperture that minimizes the sum of the residuals squared in all overlap areas simultaneously. Once the polynomial terms are computed, they are applied to each sub-aperture before all the sub-aperture data arrays are stacked with the full-aperture array, and divided by the overlap function to take the mean value at each x-y point. The sub-aperture data are then re-exported to the required interferometer format (Zygo, 4D etc.) for further analysis and use in corrective polishing or for certification purposes.



FIGURE 9. Sub-aperture interferometer being mounted prior to use. The segment under test is to the right underneath the protective cover.

TRIALS

The system has been subjected to trials on E-ELT segments where we have patched sub-apertures contained entirely within the boundary of the segment in order to address the central obstruction and CGH artefacts.

We used the fiducial scheme shown in figure 4 to fiducialize the mirror and then took 5 sub-aperture images using the interferometer system shown in figure 9. The data were stitched using 37 Zernike terms and the full segment data were output to be read back into the interferometer software.

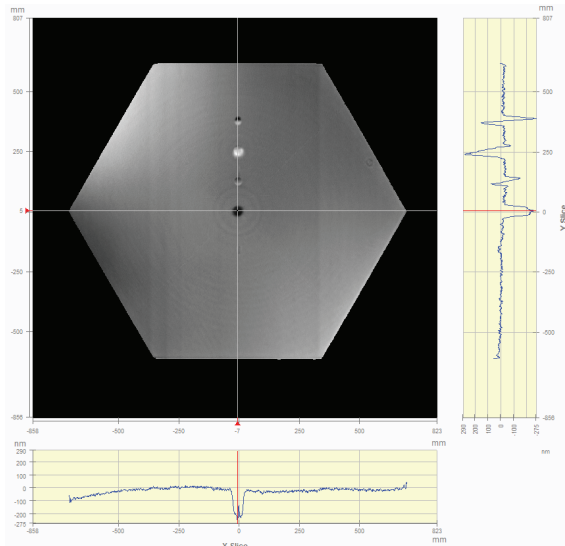


FIGURE 10. Calibrated full-aperture grayscale surface map of a segment undergoing processing. Note the artefacts induced by the fold-mirror, CGH and strays that elevate the PV and RMS values of the measurement to 565 nm and 55 nm respectively.

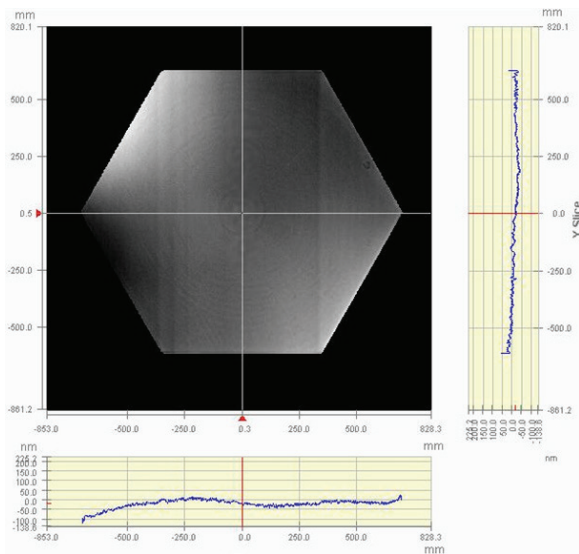


FIGURE 11. Stitched data showing the full measurement of the segment with PV and RMS values of 289 nm and 54 nm respectively.

Figures 10 and 11 show the result of the stitching operation in order to measure the surface over the problem areas near the y-axis. Prior to stitching, the areas of bad data near to the y-axis were masked out to stop them biasing the averaging using the overlap function. Note also that the values indicated in the figure

captions for PV and RMS are for raw calibrated measurements. The E-ELT segments also allow for the removal of certain low-order terms from the data after measurement to account for the segment warping that is possible when it is mounted into the telescope. Reversal tests have shown good agreement in stitched data sets, and we consider the technique useful for our work.

CONCLUSIONS AND FURTHER WORK

We have outlined our approach to fusing data from two separate interferometers to allow data patching and the fusing, and we have demonstrated the technique on off-axis aspheres where the reference information comes from a calibrated full-aperture test. The system also has applications for testing such surfaces during early figuring when the figure errors of the part are outside of the slope range of the full-aperture interferometer. We have successfully used this technique to provide patched full-aperture measurement results that fully comply with the requirements of the E-ELT telescope specifications and we now are using this technique in ongoing work producing prototype segments for this telescope.

Our future work will concentrate on the following aspects:

- 1) Increasing the accuracy of the sub-aperture registration.
- 2) Fine-tuning the fit functions in the stitching software - at present, the functions are not optimal.
- 3) Comparing the actual local surface Zernike expansion with the measurements made with the sub-aperture interferometer.
- 4) Better studies of the accuracy and ambiguities.

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Design and Manufacture of a Compound Eye System for 3D Imaging

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ABSTRACT

Digital imaging technology has been widely used in opto-electronics products and precision metrology. 3D digital imaging technology is indispensable in the testing and measurement of object's shape, profile and angular distances. This paper focuses on the design and manufacture of a compound eye system for 3D imaging. The main objective is to obtain a realistic 3D positional information of an object from a depth information extracting program. Firstly, a compound eye will be designed to collect the light field information of an image. Secondly, a depth information extracting program will be developed to derive the focus of each sub object in the image.

1. INTRODUCTION

There are many realization solutions for 3D imaging by the use of two camera units [1], camera array [2], scan camera [3] or compound eye [4] (FIGURE 1). Two camera units are indeed a stereographic system in which at least two optical systems are needed. As it is always laborious to align the compliantly of two cameras with optical precision, the 3D images constructed by these systems could not always precisely reflect the actual 3D information of objects captured. As the alignment difficulties increase with decreasing the overall dimension of the cameras system, it is also difficult to condense their sizes at a low cost. In a scanning system, a scan camera is used to collect the 3D information of an object. The scanning process, however, would require much longer capturing time on scanning the object and the resolution of the 3D imaging is sensitively affected by the scanning speed. As a result the use of scan camera in consumer products is rare and restricted.

Recently, an alternative approach using compound eye system has become increasingly popular. In this approach, a compound eye optical element is inserted into a conventional

camera at a location between the optical lenses and the CCD/CMOS. The resolution of the 3D images depends on the resolution of the CCD/CMOS and the number of the eyelets(or micro lens) on the compound eye.



FIGURE 1. Realization solutions for 3D imaging.

This paper aims at designing and manufacturing a low cost compound eye system for 3D imaging for use in consumer products. The 3D imaging theory with a compound eye is explained in Section 2, the design of the compound eye and its machining in Section 3 and the realization method of the depth information extracting program in Section 4.

2. 3D IMAGING THEORY WITH COMPOUND EYE

The working principle of a compound eye system involves capturing the light field information(LFI) of an object and then restoration of the 3D image by reconstructing the LFI . (FIGURE 2) [5].

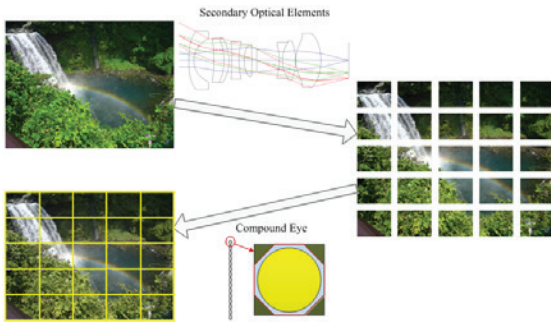


FIGURE 2. Working principle of the light field collection.

In a commercial camera, CCD/CMOS is placed at the designed focal plane of the camera lens unit, which is shown as the “Secondary Optical Elements”(FIGURE 2). For 3D imaging, the compound eye should be put between the lens unit and CCD/CMOS. The compound eye is at the designed focal plane of the lens unit, while CCD/CMOS is at the designed focal plane of the compound eye. The focal length of the compound eye is roughly equal to the designed focal length of each micro lens on the compound eye. Therefore it is easy to separate the focus and defocus situation. Furthermore the depth information of the objects can also be calculated as shown in FIGURE 3 [5].

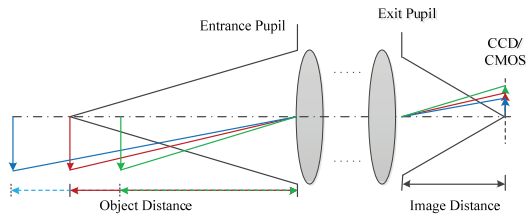


FIGURE 3. Calculation principle of the depth information among the objects.

With the depth information and traditional 2D information of the objects, 3D image of the objects can be reconstructed. This 3D image is more accurate

3. OPTICAL DESIGN AND MANUFACTURE

Regarding the cost, Canon 700D is chosen as the basic experiment platform with a camera lens unit EF 50mm f/1.4 USM. The specifications of the lens unit and CMOS are shown in TABLE 1 and TABLE 2.

TABLE 1. The specification of the camera lens unit of Canon 700D.

Focal Length & Maximum Aperture	50mm 1:1.4
Diagonal Angle of View	46°
Focus Adjustment	Overall linear extension system with USM

TABLE 2. The specification of CMOS of Canon 700D.

Resolution	5208 x 3476
Sensor size (mm)	14.9 x 22.3
Color filter array	RGB
Pixel pitch (μm)	4.3
Focal length multiplier	1.60

According to the specification of CMOS, a compound eye with circular micro lenses is designed. The focal length is 1mm and the pitch is 0.12mm. The micro lens side of the compound eye faces towards the CMOS and is directly glued on it to make sure the distance between them is smaller than the focal length of the micro lens.

In the development stage, the compound eye is made directly by machining PMMA using Nanotech 350FG attached with fast tool servo accessories. To improve the form accuracies, an optimized tool path has been developed to maintain a steady material removal rate and to minimize the effect of the change of actual tool normal rake angle in the course of freeform machining of each eyelet.(Figure 4).

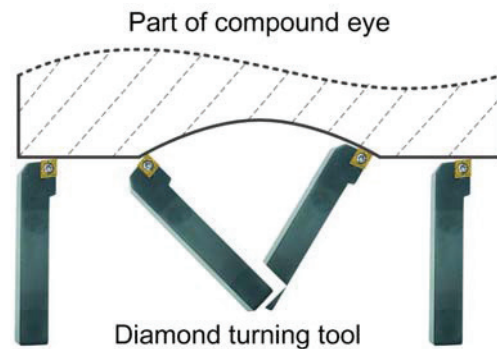


FIGURE 4. Machining schematic of the compound eye.

4. DEPTH INFORMATION EXTRACTING

There are two methods to extract the depth information. One method is based on the LFI [4], while the other one is to directly calculate the shift offset of the specified object in each pixel group under the respective micro lens [7]. In this paper, the LFI method is adopted because it will produce a better quality 3D images and with better accuracies. FIGURE 5 shows the original image generated by compound eye.



FIGURE 5. The original compound eye image.

With the LFI method, an image processing software is used to extract the depth information as follows.

- (1) Extracting the circle spots areas from the compound eye image (FIGURE 6) .

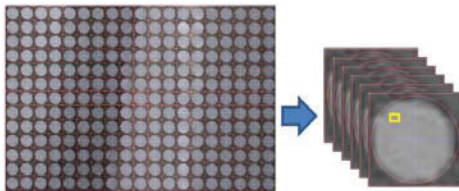


FIGURE 6. Extracting the circle spots areas.

- (2) Reconstructing the sub-images array (FIGURE 7)

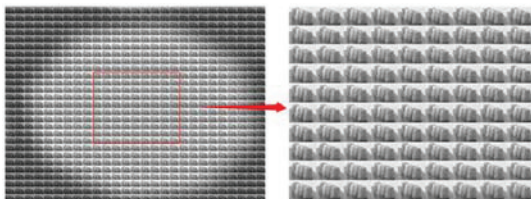


FIGURE 7. Reconstructing the sub-images array.

- (3) Calculating the depth information by adjusting the virtual imaging plan and

reconstructing the all-focus image (FIGURE 8)

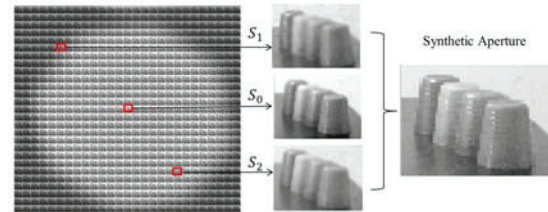


FIGURE 8. Sub-aperture images and synthetic aperture image.

In this paper, a semi-automatic method is employed which combines the automatic edge detection and manual calibration. After the extraction of sub-spots pixels, the sub-images array is reconstructed as shown in FIGURE 7. FIGURE 8 shows three sub-images reconstructed by different pixels in the sub-spot. S_1 is a sub-image reconstructed by the left-top pixels from each sub-spot, S_2 is sub-image by right-bottom pixels and S_0 is generated by the central pixels. Different sub-images generated by the pixels in different positions correspond to the virtual images of different focal plan or view angle. By the use of synthetic aperture theory, the refocused images with depth information could be obtained and be reconstructed the all-focused image.

Based on the aforementioned software developed, the sub-aperture images and the synthetic aperture image can be obtained. Thereby the 3D information of the object can be calculated. From FIGURE 9, it is easy to see that, if the two images are added together, there will be blurs in the added image. This is due to the fact that the captured objects have a displacement in two sub-aperture images. Such displacement would be more easily seen when the two images are subtracted.

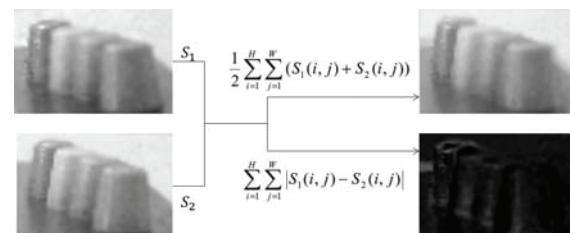


FIGURE 9. Illustration of the displacement between different sub-aperture images.

5. CONCLUSION AND DISCUSSION

In this paper, a compound eye system for 3D imaging have been designed and an extracting program of the depth information has been developed. The compound eye imaging module has been assembled and optimized with the image analytical processing software.

The compound eye insert was fabricated by machining PMMA directly using Nanotech 350FG attached with fast tool servo accessories. The quality of the insert is found to be very dependent on the steadiness of the material removal rate and on minimizing the changes of actual tool normal rake angle in the course of machining. An optimized tool path has been developed to achieve these objectives.

In future work, the optimization of the machining of the compound eye and extracting of the depth information will be focused to further improve the imaging quality.

ACKNOWLEDGMENTS

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Drive Internal Setpoint Calculation for Fast Axes

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INTRODUCTION

For many advanced optics applications such as LED automotive headlights, optics for illumination and imaging or for sensors and medical devices, complex workpiece geometries containing freeform surfaces with superimposed microstructures (hybrid optics) are needed. To machine such advanced components in a diamond turning process, a highly dynamic movement of the tool is essential. To overcome the drawbacks of state-of-the-art Slow Tool and Fast Tool systems, a new method of calculating setpoints and controlling fast axis has been developed at Fraunhofer IPT.

To achieve a direct synchronization of the tool to the spindle at high frequencies, the novel approach integrates all setpoint algorithms into a digital servo drive controller. In that way the evaluation of actual machine axis positions for synchronization, the generation of surface-based setpoints and the communication of setpoints to the Fast Tool axis are calculated within the servo cycle time simultaneously. This paper will give an insight on the novel control concept and implementation, a comparison to state-of-the-art control methods and will present first machining results.

STATE-OF-THE-ART IN DYNAMIC MOTION

Diamond machining of freeform optics can be considered as state of the art. Over the years methods of Slow Tool (or Slow Slide) machining [1], [2] or additional Fast Tool systems [2], [3] for precision machines have been developed and successfully approved for many applications. Nevertheless, both methods Slow Tool and Fast Tool have disadvantages that limit the machining accuracy and/or process speed for modern high-tech optics.

Slow Tool methods use the standard Z-axis of a precision lathe to move the infeed of the tool (FIG 1. left). All axes are controlled by the machine tool CNC, using the spindle in C-axis mode. In that way the synchronization of the tool

movement to the workpiece position on the spindle is performed by the CNC interpolator. G-Code programs are calculated offline in advance to the process and contain X, C and Z position commands. Advantages of this method are a long possible stroke due to the use of the standard Z-axis, a simple control structure and the full synchronization by the CNC controller.

As setpoints for the Slow Tool axis are calculated by the CNC, interpolation errors and limitations due to NC cycle and block calculation times reduce the achievable accuracy and dynamics. These drawbacks lead to long machining times and a strong dependence of the surface quality on the CNC algorithms.

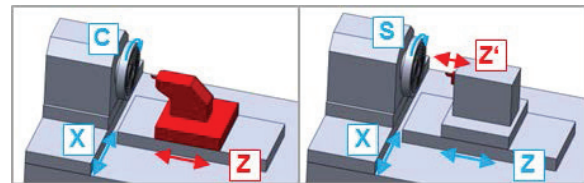


FIGURE 1. State-of-the-art Slow Tool (left) and Fast Tool (right) kinematics on a precision lathe

Fast Tool axes are additional axis (Z') mounted on the regular Z-axis of a precision lathe (FIG 1. right). To move the tool at high dynamics, piezo actuators for short stroke applications or voice coil motors for longer strokes are used. Fast Tool systems are controlled in a master/slave control mode. For the synchronization of the tool movement to the workpiece position on the spindle, the position sensor signals of the machine axes (X, S) are split by Y-splitters and communicated to an external controller. With these signals the tool infeed is calculated online using the real surface information of the workpiece geometry and communicated to the Fast Tool controller. Due to the short stroke and a low moving mass, high frequency movements of the Fast Tool are possible.

As Fast Tool systems are controlled by external controllers, close attention has to be paid to communication, synchronization and safety issues. Signal propagation times and calculation algorithms lead to latencies and jitter.

CHALLENGES OF HIGHTECH OPTICS

The evolution of optics manufacturing involves diamond machining of spheres, aspheres, freeform optics and microstructured optical surfaces. Most of these components are to some extent symmetrical or the freeform parts have no dedicated orientation. So they are rather easy to machine with Slow Tool and/or Fast Tool methods. In modern applications such as LED headlights for the automotive industry a combination of freeform geometries and superimposed microstructures are required to meet both functional and aesthetical design aspects. Such optics have a demand for a certain orientation, either for the structure to the freeform or for the surface as a whole to a reference mark (FIG 2.).

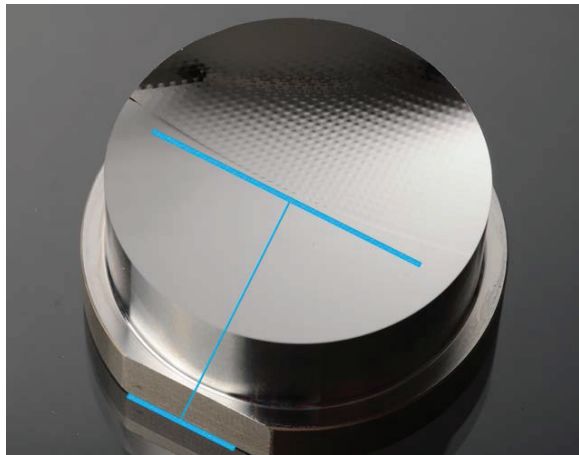


FIGURE 2. *Superimposed microstructures on a freeform surface machined with orientation to a reference mark (for adjusted mounting)*

With these demands new machining challenges arise in terms of synchronizing the tool movement to the workpiece position on the spindle. Starting and stopping the Fast Tool infeed movement at a certain angle requires accuracies in the range of single micro degrees, because large parts require micrometer precision even at the outer radius of the workpiece chuck. FIG 3. shows a schematic drawing of a large-sized workpiece (HUD master) including the parameters to calculate the desired control accuracy.

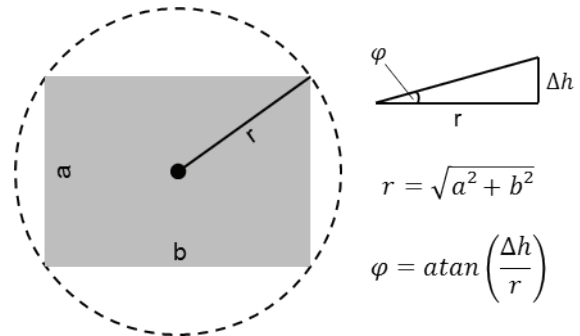


FIGURE 3. *Geometric parameters of a large rectangular workpiece (head-up display master)*

To achieve the desired contour accuracy of microstructured or oriented freeform surfaces even for large-scaled workpieces, synchronizing the tool movement to the spindle position is essential. Taking the working area radius and the desired feature size (and contour accuracy) into account, the maximum allowed angle deviation can be calculated by the following formula (compare FIG 3.):

$$\varphi = \operatorname{atan}\left(\frac{\Delta h}{r}\right)$$

For a workpiece dimension of 350x250 mm² (compare FIG 5.) a working area radius of approx. 430 mm results. The desired contour accuracy over the whole surface (critical at the outer radius) for this application is $\pm 3 \mu\text{m}$. With these values the maximum angle deviation for synchronization calculates to $800 \mu^\circ$. Using the work spindle speed, the maximum delay time for a setpoint generator (including calculation, interpolation and communication of setpoints) can be calculated by the following formula:

$$\Delta s = \frac{\varphi}{n}$$

For the above calculated angle deviation of $800 \mu^\circ$ and a spindle speed of 500 rpm a maximum delay time of 266 ns results. As a constant delay time (latency) can only be compensated in a sufficient way if it is deterministic, the deviation of that delay time (jitter) has to be reduced to a minimum. Taking the Nyquist criterion [4] into account for the calculated values, a setpoint generator for applications like the above mentioned has to be realized with a jitter smaller than 133 ns. This can be achieved by using digital, FPGA-based servo drives and implementing a drive internal setpoint generation [5], [6].

DRIVE INTERNAL SETPOINT GENERATION

To achieve a direct synchronization at high frequencies, the novel approach integrates all setpoint algorithms into a digital servo drive controller. In that way the evaluation of actual machine axis positions for synchronization, the generation of surface-based setpoints and the communication of setpoints to the Fast Tool axis are calculated within the servo cycle time simultaneously. In more detail, the actual position of the X-axis and the angle of the work spindle are used to refer to the actual tool position projection onto the surface of the workpiece. They are transformed into surface-based coordinates (e.g. NURBS). With the position in surface coordinates the infeed for the diamond tool is calculated and transformed back into machine coordinates. The infeed position is used as a setpoint command for the tool axis. Implementing these algorithms within the digital servo drive all calculations are executed synchronized and at the servo cycle frequency of 10 kHz.

To execute the surface evaluation calculations within that cycle time, the amount of data has to be reduced by using a look-ahead algorithm. With the actual spindle position and rotational speed a section of the surface description that describes the upcoming area to be machined can be extracted. This actual surface section is loaded from the surface data file into the realtime kernel of the motion controller (FIG 4.).

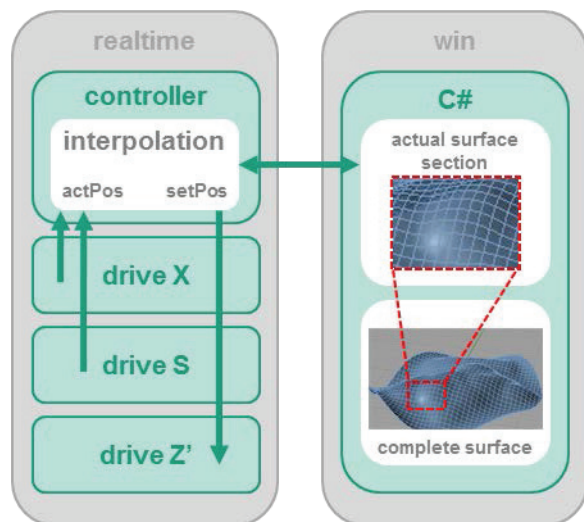


FIGURE 4. Schematic drawing of the novel approach of drive internal setpoint generation – multiaxis controller for standard machine axes and highly dynamic tool axis (left), C# surface pre-evaluation (right)

Within the servo loop (10 kHz) the actual surface fragment is evaluated to calculate the tool infeed. To achieve a smooth and continuous data handling, two buffers are used to communicate surface data between the windows and realtime environment. The first buffer is used to run the calculations of the actual surface area for interpolating the tool path. Simultaneously the second buffer loads the next surface fragment. While the interpolator switches to the second buffer, the first buffer loads the next surface information. Using alternating buffers in that way, large surface descriptions (several GB) can be computed in realtime, as only a small segment is used for the calculations within the servo loop.

Compared to state-of-the-art approaches very low latencies can be achieved (due to the fast algorithms within the motion controller) and CNC interpolation errors or limitations in dynamics can be avoided. Furthermore, all axes including the Fast Tool axis are controlled by the same controller, which allows for a continuous safety concept such as in conventional machine tools. Finally the machine operator does not have to deal with alignment, synchronization or Fast Tool programming issues, as they are calculated automatically. Using modern digital servo drive controllers a setpoint frequency of 10 kHz with a resolution of 1 nm can be achieved. The position and current control loops are executed at 10 kHz respectively 100 kHz. Using the new control method an efficient and easy to operate machining of complex hybrid optical components becomes possible.

Combining the advantages of Slow Tool methods (closed interpolation of all involved axes) and Fast Tool systems (fast calculation times and spindle mode result in higher dynamics), drive internal setpoint calculation allows for ultra-precise and simultaneously highly dynamic motion control of the tool axis synchronized to the workpiece position of the spindle. Furthermore, using an ironless linear motor for the fast axis a large stroke and at the same time a high-frequency movement can be achieved.

MACHINING RESULTS

FIG 5. shows an example of a workpiece that has been machined with the novel control approach. The upper picture displays the machined surface of a head-up display master with dimensions of $350 \times 250 \text{ mm}^2$ (compare calculations above in FIG 3.). The surface description is shown as a MATLAB plot in the lower figure. The demanded accuracy to achieve full optical functionality is $\pm 3 \mu\text{m}$ for the whole surface. The critical parts are at the outer radius, where the tool leaves and reenters the workpiece (interrupted cut).

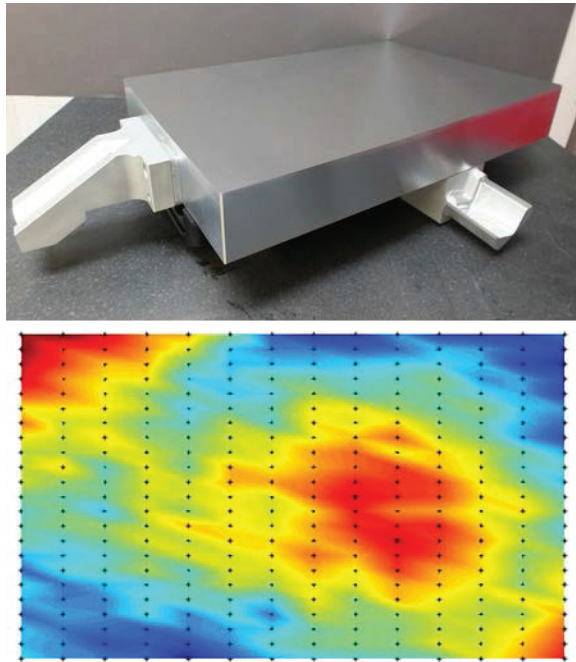


FIGURE 5. Machining result: head-up display master (dimensions $350 \times 250 \text{ mm}^2$) – master workpiece (top), surface description (bottom)

FIG 6. shows a surface measurement of the machined HUD master. The machined surface has a roughness of 5.7 nm Ra . The form accuracy meets the tolerances of $\pm 3 \mu\text{m}$.

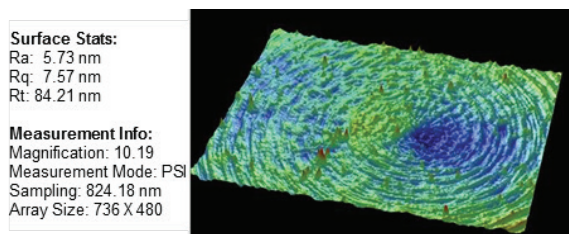


FIGURE 6. Surface measurement of the machined head-up display master

SUMMARY AND OUTLOOK

Optics for high-tech applications currently combine freeform surfaces and microstructures and have a demand for a certain orientation of the structure to reference marks for mounting. State-of-the-art Slow Tool and Fast Tool methods both have drawbacks that either limit the achievable process dynamics or the desired accuracy. At Fraunhofer IPT a novel control method has been developed implementing a drive internal setpoint calculation strategy for fast axes.

The new approach uses parts of the original geometry description to calculate surface-based setpoints within the fast servo loop of the motion controller at 10 kHz. With this method, a closed and continuous interpolation of all axes (X, Z, C) is realized (one advantage of Slow Tool methods). On the other hand, the evaluation of the actual workpiece position and the calculation of setpoints for the tool axis on this basis can be executed a shorter cycle times like in Fast Tool systems. Combining the advantages of both methods, a very low latency setpoint generator can be implemented guaranteeing ultra-precise and highly dynamic tool movements.

Future work will focus on further machining tests and measurements of the achievable accuracy.

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A STUDY OF PROCESS CHAIN MODELLING AND ANALYSIS FOR COMPUTER CONTROLLED ULTRA-PRECISION POLISHING OF STRUCTURED FREEFORM SURFACES

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ABSTRACT

This paper presents a study of process chain modelling and analysis for computer controlled ultra-precision polishing of structured freeform surfaces. This includes a study of the effect of different machining process steps on the surface generation in CCUP. A prototype process chain modeling and optimization system is established to analyze the effect of the design of process chain of machining processes on the surface generation in CCUP. Preliminary experimental work is undertaken to validate the system performance. The results show that the structured surface generation in succeeding polishing process step is affected by the residual surface topography in preceding polishing process step. There exists an optimum polishing condition at which the effect of residual surface generated in pre-polishing process is minimized in structured surface generation by CCUP. The findings not only provide an important means for better understanding of theory and science of the effect on surface generation due to design and planning of the polishing processes but also allow the determination of optimum polishing conditions for structured surface generation in CCUP.

KEYWORDS: Polishing, Structured Freeform Surface, Ultra-precision Machining, Process Chain Modelling, Surface Generation

INTRODUCTION

Structured freeform surfaces are surfaces possessing specially designed functional textures which are widely used in the development of a wide range of products. Over the past decades, surfaces possessing specially-designed 3D-structures in optical science have been found to perform desirable optical functions [1] such as micro-lens array, v-grooves, etc. They are widely used in large screen LCD backlight module and LED ceiling light panel. Computer Controlled Ultra-precision Polishing (CCUP) provides an enabling technology for machining structured freeform surfaces. It can be used to machine difficult-to-machine materials such as optical glass which are not amenable for diamond

machining. Due to the complexity of material removal mechanism in the polishing process, a clear understanding of the material removal rate (MRR) or influence function are much needed to better control the polishing process precisely.

Successful modelling and simulation of the polishing process are always the ways to understand the process, improve the overall performance, avoid trial-and-error and enable the process to be more controllable and predictable. Hence, there is a need to model and simulate the surface generation process. The polishing process is affected by more than one factor and material removal mechanism. This increases the difficulty in constructing models for them. The most well-known semi-empirical equation that describes the parameters in the process was established by Preston [2], which is named Preston's equation based on an assumption that a number of granules of the same size penetrates the ideal plane of the part in elastic Hertzian fashion and in shear, and ploughs a groove with a cross-section that is proportional to that of the penetration [3].

As Preston's equation can only reflect the influence of load and relative velocity, a number of researchers have made a lot of effort to modify it. In mechanical polishing, the polishing heads have direct contact with the workpiece. Some attempts have been made in building models based on the shape of polishing tools used in the process. Xie et al. [4] built a model based on the geometry of the bonnet and Preston equation and determined the linear relationship between material removal depth and dwell time. Their model is a semi-empirical model as the Preston coefficient had to be found from the experimental data. Guiot et al. [5] developed a model to estimate the material removal rate along a polishing tool path based on the contact area and the contact pressure between the tool and the workpiece. Zheng and a group of researchers [6] investigated the impact on material removal rate according to the geometry and dimension of polishing pad.

Although the current models are appropriate for simulating the material removal process, they only focus on modelling and simulation of a local area but not a large topography of surface. For ultra-precision polishing, there is a need for the development of a deterministic model to predict and simulate the surface after polishing under a particular set of parameters. On the other hands, it is interesting to note that the surface generation cannot be generally be achieved by a single polishing process step. In other words, it is usually realized by a series of polishing process steps. The surface generation in the current polishing process step is affected by that of the previous polishing process steps. However, most of the previous studies focus mainly on individual polishing process while the effect of the interaction of each of different processes and steps has received relatively little attention.

As a result, this paper presents a study of process chain modelling for computer controlled ultra-precision polishing of structured freeform surfaces. This includes a study of the effect of different machining process steps on the surface generation in CCUP. A prototype process chain modeling and optimization system is established based on the previous work of authors to analyze the effect of various factors affecting the surface generation in CCUP. Preliminary experimental work is undertaken to validate the system performance.

A FRAMEWORK OF A PROCESS CHAIN MODELLING AND OPTIMIZATION SYSTEM

FIGURE 1 shows a framework of a process chain modelling and optimization system for polishing process design, which consists of three modules: Input Module, Structured Freeform Surface Generation Model, Process Planning and Optimization Model and Output Module. The input module includes some necessary information such as workpiece material, initial surface roughness and expected roughness after the optimization of process planning based on modeling and simulation approach.

The structured freeform surface generation model is built based on the previous work by the author [7] on the study of influence function, polishing tool path, to simulate and predict the structured freeform surface generation during the polishing process. Different from the previous surface generation model [7], the present model takes into account the surface topography (texture, pattern), which is generated from the previous

process. The generated surface also involves the simulated results provide as the pre-polishing surface. In this means, different processes in a designed sequence produce surface topography by the modeling and simulation approach.

In the process planning and optimization model, it takes into account the surface topography (texture, pattern), which is generated from the previous process. The simulated results provide as the pre-polishing surface. In this means, different process in a designed sequence produce surface topography by the modeling and simulation approach, and avoid the trial-error tests. Different types of polishing processes are selected or defined first such as mechanical polishing (MP) and fluid jet polishing (FJP). Influence function of each polishing type is determined based on trial polishing experiments. With different polishing parameters, these different types of polishing processes also generate various polishing process chain, which can be expressed as:

$$Pp = \{MP_1, MP_2, \dots, MP_m; FJP_1, FJP_2, \dots, FJP_n\} \quad (1)$$

Then the process planning, by arranging the polishing steps in different sequence and also with different polishing particles, can be expressed as

$$Pl = \{P_1, P_2, \dots, P_k, \dots, P_l\}, P_k \in Pp \quad (2)$$

In the process planning, each polishing step P_1 generates its surface polishing results in terms of surface roughness (RF_1), which serve as the pre-surface topography for the next step. For one process planning, it can be expressed as

$$P_1 / RF_1 \rightarrow P_2 / RF_2 \rightarrow \dots \rightarrow P_k / RF_k \rightarrow \dots \rightarrow P_l / RF_l \quad (3)$$

The final predicted P_l / RF_l is used for the evaluation of the results of the designed polishing process planning. During each process plan, the previous surface topography affects the material removal rate or influence function of the succeeding process. As a result, surface regeneration in polishing process is investigated. As shown FIGURE 2(a), the modeling of polishing influence function of the current process takes into account the polished results from previous polishing process steps. FIGURE 2(b) is the surface regeneration which happens during one polishing process step by means of

repeated polishing at the same area.

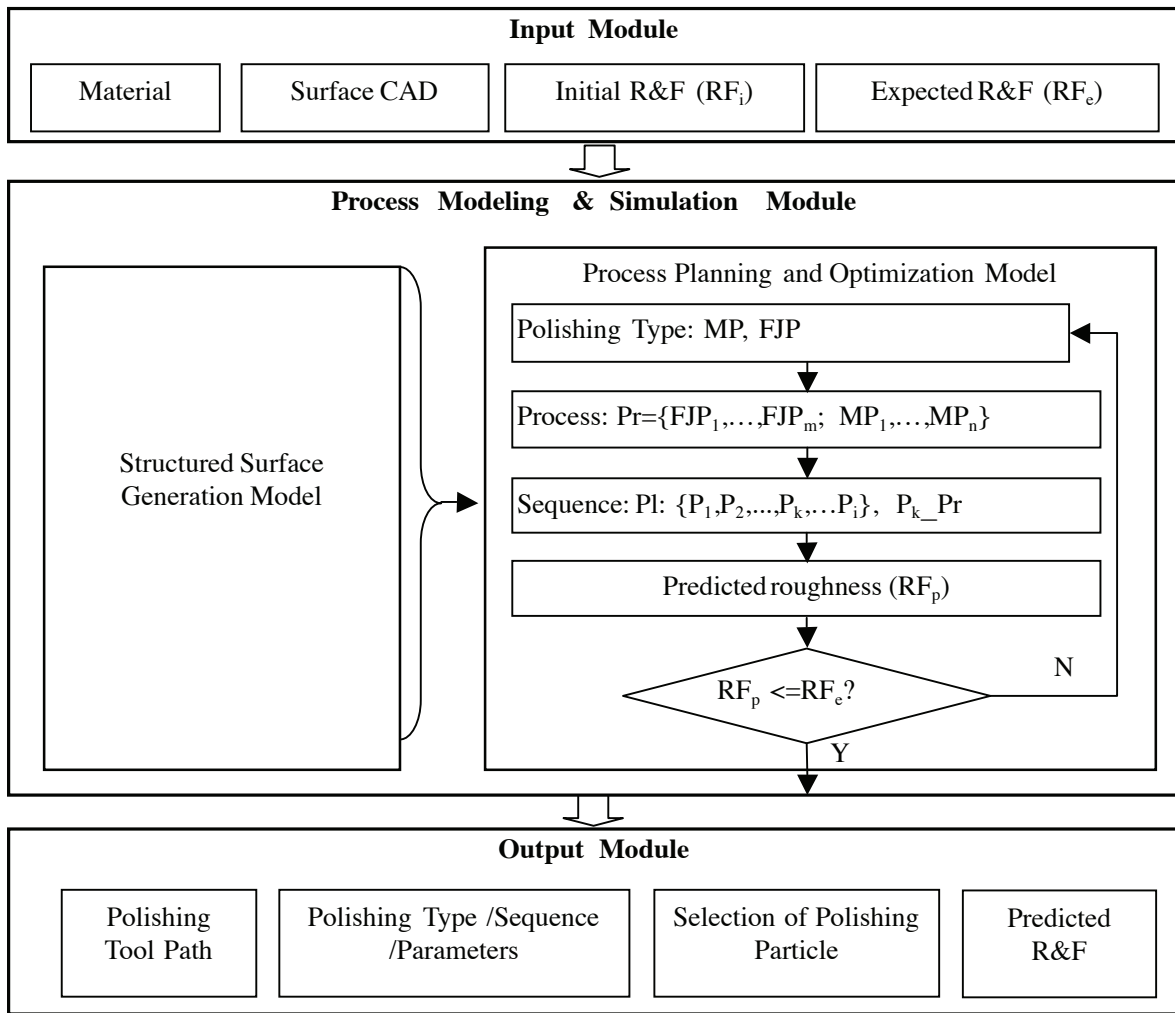


FIGURE 1. A Framework of a Process Chain Modelling and Optimization System.

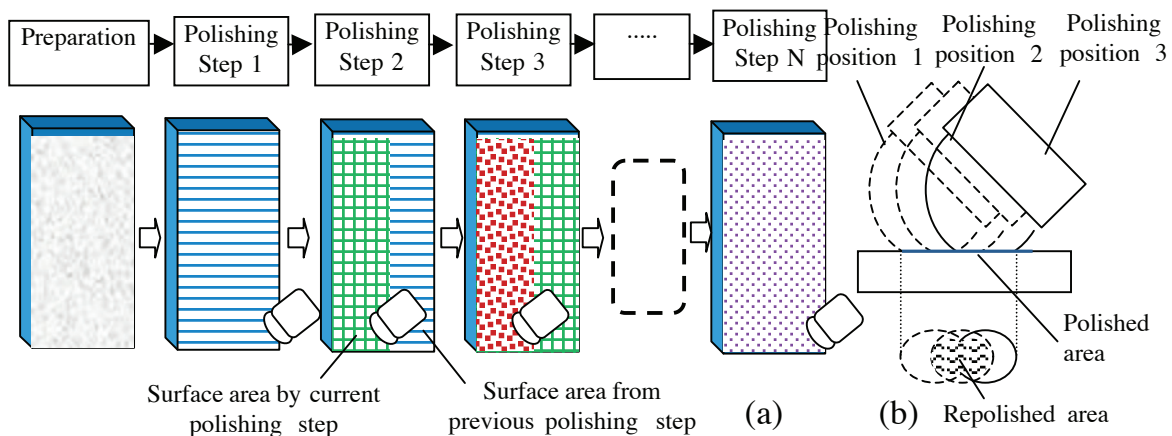


FIGURE 2 Illustration for surface regeneration during polishing process due to: (a) Polishing sequence for different steps; (b) Repolishing in one step.

EXPERIMENTAL WORK

In the present study, a prototype of process modelling and optimization system has been

built by using MATLAB programming language. To preliminary validate the system, a series of simulation and actual machining

experiments have been undertaken to realize the effect of process chain design on the structured surface generation.

Simulation Experiments

A flat workpiece surface is firstly machined by Single Point Diamond Turning (SPDT), and is then polished by mechanical polishing (MP) followed by fluid jet polishing (FJP). FIGURE 3 shows the simulated surface topography of the surface machined by SPDT while FIGURE 4 shows the structured surface generated after mechanical polishing with a certain surface feed rate. It is interesting to note that the residual machining marks generated from the previous SPDT process still remains. This indicates that there exists a minimum feed rate to ensure sufficient dwelling time of the polishing process is available to remove the residual machining mark from the previous process. The finding provides a helpful means to determine the machining parameters in the design of the process chain so as to strike a well balance between the expected surface generation and the machining efficiency (cost-effect balance). FIGURE 5 shows the simulated results after SPDT, MP and FJP with certain machining parameters. The combined effect from different machining processes after machining is shown. It is found that the simulated surface topography inherits the surface features generated in different steps of the process chain. The results infers that there is a need to incorporate the residual surface topography from the preceding machining process in the modelling of the surface generation in the succeeding machining process in the process chain modelling of the machining processes.

Actual Machining Experiments

Actual machining experiments were also designed to model a process chain in the present study. Nickel Copper (NiCu) samples with a diameter of 25mm, were firstly machined by Single Point Diamond Turning (SPDT), and then Mechanical Polishing (MP) followed by Fluid Jet Polishing (FJP). The machining parameters in each process are shown in Table 1. Table 2 shows the simulated and machined structured surface generation after each process. For the mechanical and fluid jet polishing, the feed rates were designed which are used in common polishing process. From the results, it interesting to note that each succeeding machining process has completely removed the machining marks from preceding machining process, which indicates the feed rates of MP and FJP are both below the

minimum feed rate value. In the future studies, this minimum feed rate or critical machining parameters will be further investigated.

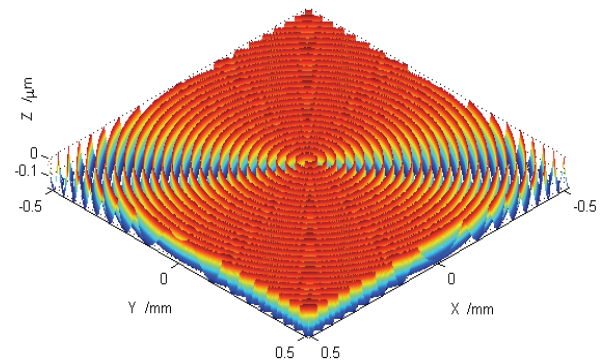


FIGURE 3. Simulated surface topography of the surface machined by Single Point Diamond Turning (SPDT).

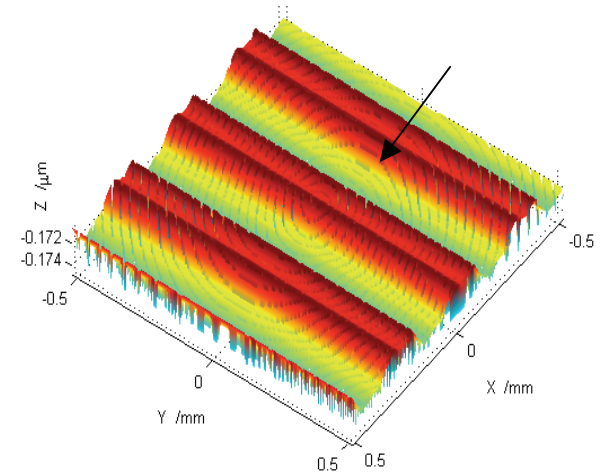


FIGURE 4. Structured surface generated by mechanical polishing (MP) when feed rate within a certain range but over minimum value.

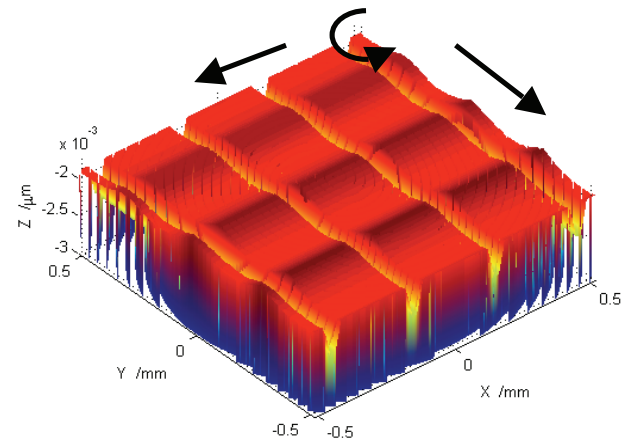
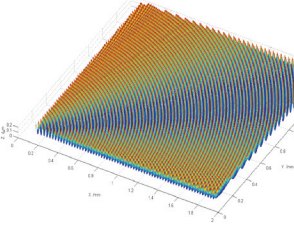
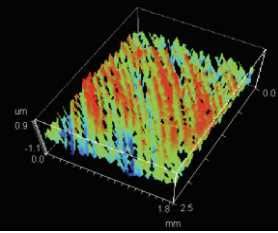
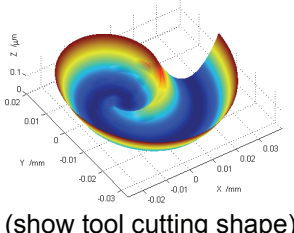
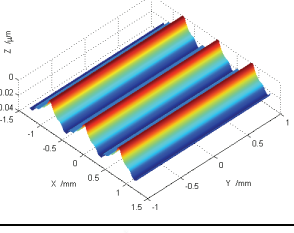
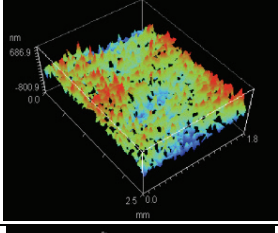
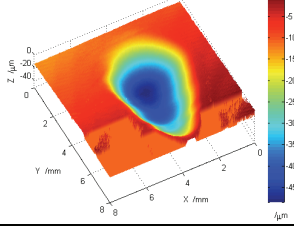
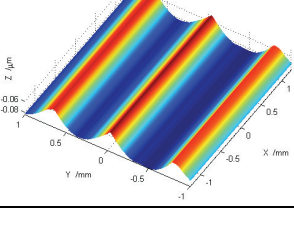
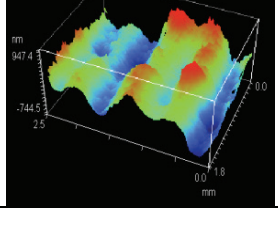
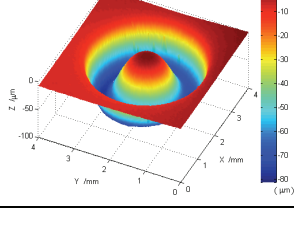


FIGURE 5. Surface generation after SPDT, MP and FJP.

TABLE 1. Design of the experiments and machining conditions.

Step	Machining method	Machining Parameters	
1	Single Point Diamond Turning (SPDT)	Spindle speed (rpm)	1000
		Feed rate (mm/min)	26.5
		Depth of cut (μm)	10
		Tool radius, r_t (mm)	0.5
2	Mechanical Polishing (MP)	Inclination angle ($^\circ$)	10
		Spindle speed (rpm)	1200
		Tool offset (mm)	0.3
		Tool pressure (bar)	1
		Surface feed (mm/min)	30
		Tool radius (mm)	20
		XY spacing (mm)	0.8
		Polishing cloth	Cerium oxide D'16
		Polishing mode	Raster
2	Fluid Jet Polishing (FJP)	Polishing slurry	Al_2O_3
		Inclination angle ($^\circ$)	0
		Tool offset (mm)	10
		Jet pressure (bar)	9-10
		Surface feed (mm/min)	15
		Nozzle diameter (mm)	0.75
		XY spacing (mm)	0.7
		Polishing mode	Raster
		Polishing slurry	Al_2O_3

TABLE 2. Experimental test results for surface generation in process chain

Step	Process	Simulation Results	Measured Results	TIF
1	SPTD			 (show tool cutting shape)
2	MP			
3	FJP			

CONCLUSION

Structured freeform surfaces are surfaces possessing specially designed functional textures which are widely used in the development of a wide range of products. In computer controlled ultra-precision polishing

(CCUP), the structured surface generation is usually realized by a series of polishing process steps. However, most of the previous studies focus mainly on individual polishing process while the effect of the interaction of each of different processes and steps has

received relatively little attention.

In this paper, the effect of process chain design on the structured surface generation is studied. A prototype process chain modelling and optimization system has been built and a series of simulation and actual machining experiments have been conducted. The results show that the structured surface generation in succeeding polishing process step is susceptible by that of the preceding polishing process step. It is also found that there exists an optimized polishing condition in minimizing the effect of residual surface generated in the pre-polishing process on the structured generation in CCUP. The success of the development of the process chain modelling and optimization system not only provides a better understanding of theory and science of materials removal mechanisms but also their effect on surface generation due to design and planning of the polishing processes.

ACKNOWLEDGEMENT

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COMPACT DUAL-INTERFEROMETER FOR MEASURING LENS CENTRATION AND WEDGE

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INTRODUCTION

Molded lens technology provides a means of reducing the number of surfaces in an optical system by using aspheres on one or both lens surfaces. While doing this improves the performance of the lens system, it complicates the manufacturing because the axes of the aspheres should be coincident in a perfect lens. To adjust the molding process to make the axes coincident metrology to ascertain the degree of tilt and decenter between the axes is needed.

We discuss an optical means of making the measurement using a pair of facing interferometers, show a method of calculating the relationship of the axes, some experimental results and the limitations of the method.

THEORY

For a lens with spherical surfaces the definition of the optical axis is unequivocal; it is the line joining the centers of curvature of the two surfaces. For a lens with one or two aspheric surfaces the definition to first order is the same, the line joining the centers of curvature of the base, or paraxial, spherical surface

In reality, each aspheric surface has an optical axis which may be defined as the line between the center of curvature of the surface, defined above, and its vertex, as shown in Fig. 1. The optical axis of an aspheric surface is also the rotational axis of symmetry of the surface, as well as the line containing the center of curvature of each annular zone of the surface. Note that spherical surfaces have no optical vertex because there is no unique point of symmetry. They do have a mechanical vertex defined by the periphery of the lens.

Several things are obvious in Fig.1. The optical and mechanical axes are not parallel so the optical axis is tilted relative to the mechanical. Also, the vertices of the two surfaces are decentered relative to each other. If we assume

our reference system for metrology is parallel to the mechanical axis of the lens and centered on its center of curvature, it is clear the lens can be rotated about the left surface center of curvature by the difference in the angle of the two axes to give a situation like that shown in Fig. 2.

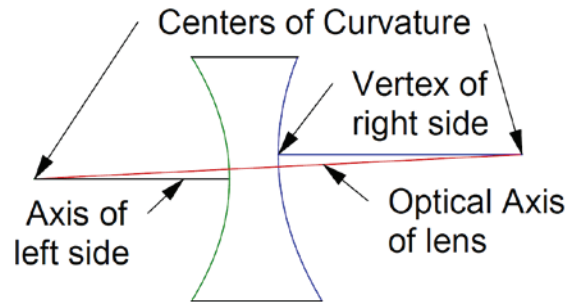


FIGURE 1. Definitions of the optical axis of a lens and the axes of the two aspheric sides

It is assumed that the tilt between a lens surface and the corresponding reference surface is small compared to the decenter. Each reference surface corresponds to the planar reference surface in one of the Mirau objectives discussed below. The reference surfaces are nominally parallel and can be calibrated using for instance, reversal techniques. Finally, the decenter of the vertices is perpendicular to the optical axis.

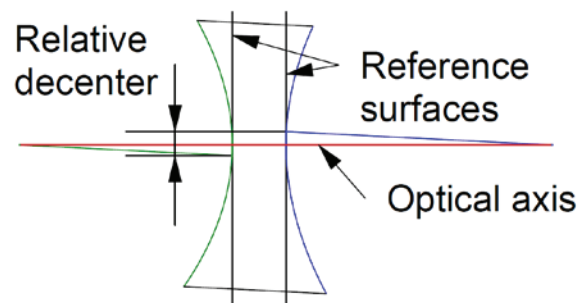


FIGURE 2. Lens rotated about the center of curvature of the left surface so the optical axis is normal to the reference surface.

From a manufacturing viewpoint it could be thought that the mold inserts could be tilted and thus change the tilt between the surfaces, but the tilt would have the effect of moving the centers of curvature and therefore the optical axis. Once the optical axis of one of the surfaces is made normal to the reference surface the only error is decenter.

Since there is only decenter to measure once the lens is tilted normal to the reference surface in the objective the question is how to measure the decenter. To do this we must calculate the difference between a spherical reference and the aspheric lens surface. The exact solution is messy so we will illustrate this to 3rd order. The sag of a conic curve is given by

$$sag = \frac{cy^2}{1 + \sqrt{1 - (k+1)c^2y^2}}$$

where $c = 1/R_v$ where R_v is the vertex radius of the surface and k is the conic constant ($k = 0$ for a sphere). Expanding this conic sag less the sag of a sphere of the same vertex radius in a power series gives the difference as

$$\Delta sag = \frac{kc^3y^4}{8} + \dots$$

Clearly there is no difference if the surface is a sphere where $k = 0$. If the surface is decentered then $\Delta sag(h)$ is a function of the decenter h . Expanding we have

$$\begin{aligned} \Delta sag &= \frac{kc^3(y-h)^4}{8} \\ &= \frac{kc^3(y^4 - 4y^3h + 6y^2h^2 - 4yh^3 + h^4)}{8} \end{aligned}$$

This shows the decentered surface has a small deviation proportional to y^3 as well as smaller, higher order terms in addition to the y^4 dependence. It is the presence of this y^3 character that allows us to solve quantitatively for the decenter using optical means. The y^3 behavior is referred to as coma in optical terms and it is necessary to use an optical design program to get the exact value of the coma versus decenter rather than using this illustrative example.

MEASUREMENT

To measure the departure from sphere and the tilt and decenter we use a pair of Mirau objective based interferometers like those shown in Fig. 3. In a Mirau objective the interference takes place between a plane reference mirror in the objective and the surface under test. The “interferometer” is the objective and the rest of the instrument is the LED quasi-monochromatic light source and the CCD detector. The pair of interferometers face each other with their focal planes and foci coincident.

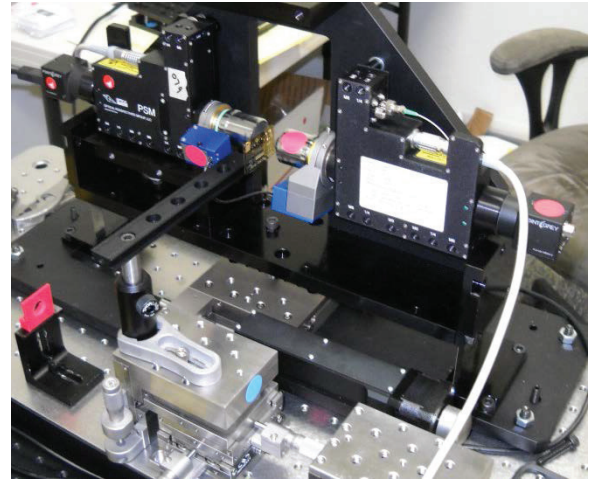


FIGURE 3. Pair of Mirau interferometers facing each other with lens under test mounted between them.

The frame holding the two interferometers is mounted on a single axis flexure stage so that the pair may be moved from being in focus on one side of the lens to the other. This also aids in setting the two interferometers so their reference planes parallel to each other because a thin parallel plate can be mounted in place of the lens and adjusted for no tilt. Then when focused on the other side of the plate the second interferometer is adjusted for no tilt.

The built in point sources of the Point Source Microscopes¹ and the centroiding software that is a part of them is used during the setting of the coincidence of the two foci. The point source in one is seen by the second and vice versa. This permits sub-micron lateral positioning of the foci.

MEASUREMENT RESULTS

To demonstrate how the interferometric measurement works to find the decenter we used the dual interferometer to measure the

front surface of a lens. The interferogram of the surface with little tilt and decenter is shown in Fig.4.

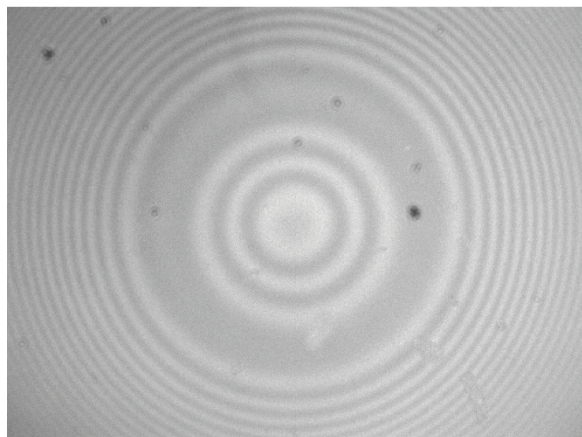


FIGURE 4. Interferogram of front lens surface

The interferogram was converted into a topographic map of the surface and Zernike polynomials were fit over a circular aperture whose diameter was the narrow dimension of Fig. 4. Using the Zernike coefficients we got the useful results shown in Fig. 5. Here the tilt and 3rd order coma data lie on top of one another and show a decenter of about 0.4 μm .

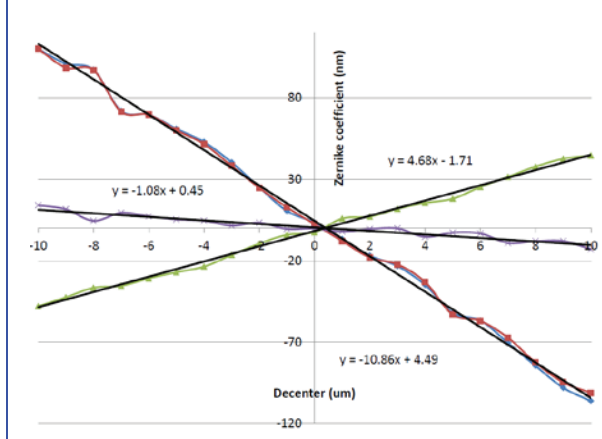


FIGURE 5. Plot of decenter versus Zernike coefficient value in nm for tilt and 3rd order coma (red and blue), 5th order coma (green) and 7th order coma (gray).

This measurement was particularly satisfying in that the surface had sufficient aspheric departure that there was not only 3rd order coma, but 5th and 7th order as well. Further, all 3 orders showed a zero value at $0.4 \pm 0.2 \mu\text{m}$ of decenter, an indication of how precise the

decenter could be determined for this particular surface.

Several things contributed to the precision of this measurement. The radius of curvature of the surface was quite long so a full mm diameter of the surface could be measured with close to a million data points within the coherence length of the LED light source. Further, the large departure from a sphere gave a high sensitivity to coma. Finally, the long radius meant that the numerical aperture (NA) of the Mirau objective did not have to be high to capture all the reflected light from the surface.

The second side of the lens was not as cooperative as shown in a similar plot in Fig. 5. Here the plot is over the same range of decenter but the tilt coefficient is more than 250 times greater than either the 3rd or 5th order coma coefficients. This is due to the relatively short radius of curvature of the second side of the lens. Further, because over the portion of the lens that could be measured due to the short radius, the surface was very nearly spherical so there was very little coma. Here the decenter predicted by the coma were 9.2 and 7.7 μm , hardly the sub-micron precision that would be desired. The steep radius also required us to use a higher NA objective to see as much of the surface as possible. This is a tradeoff since the higher NA objective also limits the field of view.

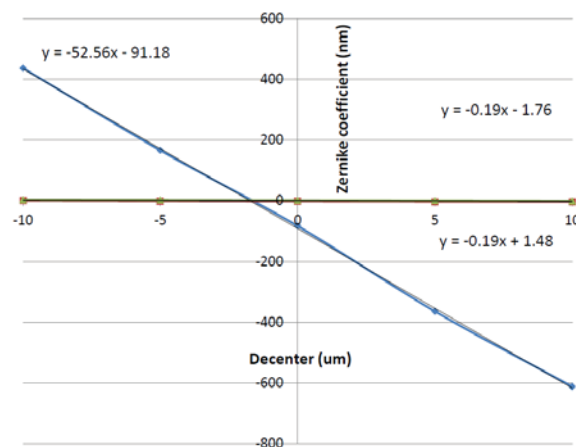


FIGURE 6. Plot of decenter versus Zernike coefficient for other side of lens shown in Fig. 5.

At this point one could argue that since this surface is so close to spherical that the vertex location does not matter. In this case, however, the surface was highly aspheric outside the

region that could be seen since we were limited by the short radius of curvature. For some surfaces it is necessary to measure substantially beyond the center of the lens to pick up the aspheric departure and find the vertex of the surface. It is for this reason the limitations of the method are important to understand.

LIMITATIONS OF THE METHOD

Our experimental results clearly show the limitations of the micro-interferometry of small molded lenses depending on the design parameters of the lens. We now discuss a few approaches to improve the test method.

For phase shifting interferometry the departure of the measured surface from a plane surface, the reference in the Mirau objective, is limited to about 6 μm by the coherence of the light source being used, in our case a red LED. This can be improved somewhat by using a filter.

Another solution would be to use white light scanning interferometry where departures from a plane of 100 to 200 μm would not be an issue assuming the objective had a sufficiently large numerical aperture to capture the reflected light. The precision of the measurement at each pixel would be perhaps five times worse than in the case of phase shifting but the surfaces being measured are smooth and continuous. In addition there is an immense amount of data that can be used to fit the polynomials that give the centering data. Thus white light scanning seems to be a next obvious way of making improvement.

There is also the question of the shape of the surface being measured. If it has a relatively long radius and large aspheric departure as in the first example, the Mirau objective where the surface is compared to a plane is a good solution. Where the surface has a short radius as in the second example it would make more sense to compare the surface with a spherical reference of somewhat the same radius of curvature. This could be done with a Linnik objective and a spherical reference surface. Now the interference pattern is just the departure between a sphere and the asphere under test. Assuming the objectives used have a sufficient NA it should be possible to measure any short radius surface as long as the departure does not exceed the coherence length of the light source.

CONCLUSIONS

We have shown that it is possible to determine the decenter of small, molded aspheric lenses using a dual set of facing interferometers provided the lens surfaces meet certain characteristics. Further we have shown that where the surfaces are not amenable to the method described there are potential solutions that would make it possible to measure the decentration to sub-micron levels for all or most lens surface.

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FIGURE CORRECTION OF FREEFORM MIRRORS WITH WELL-DEFINED REFERENCE STRUCTURES BY MRF

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INTRODUCTION

The outstanding improvements that can be achieved by incorporating aspheric and freeform shaped mirrors into today's high-end optical systems, e.g. for space and ground-based astronomy, require for fast and deterministic manufacturing and metrology approaches. Especially high-precision metrology during iterative polishing and the integration of freeforms into an optical system are time consuming, expensive, and error-prone tasks in the overall process chain. Reference elements located outside of the clear aperture on an optical surface are mandatory to describe and detect the optical coordinate system of a complex geometry during metrology or system integration [1]. Ultra-precision diamond machining techniques are well-suited for the fabrication of complex optical geometries as well as for a broad range of very flat or steep curved reference elements [2].

Figure 1 shows exemplary the primary mirror of an all-aluminum Korsch-type afocal telescope with ultra-precisely manufactured reference elements and mounting interfaces. The off-axis geometry of the mirror was mathematically transformed in order to diamond turn the optics on-axis as a freeform with a Fast Tool Servo approach [3]. Diamond milled concave spheres were fabricated outside of the mirrors clear aperture. They embody the optical coordinate system absolutely in all six degrees of freedom and are used for freeform metrology during the fabrication cycle. Since all reference marks and the optical surface were manufactured in a common machine setting, position deviations less than a micron between the optical freeform and the reference elements were achieved. A sophisticated process chain based on ultra-precision diamond machining and the utilization of reference structures was successfully established for snap-together all-metal telescopes working in the infrared spectral range.



FIGURE 1. Light-weighted aspheric aluminum mirror with diamond milled reference elements for interferometric and tactile metrology and ultra-precisely manufactured mounting interfaces for system integration.

However, optical telescopes working at shorter wavelengths, e.g. for multi- or hyperspectral remote sensing applications in the visible spectral range, require optical elements with enhanced figure deviations and better roughness. The paper describes the figure correction of diamond turned freeform mirrors with Magnetorheological Finishing (MRF) according to precisely manufactured reference elements. Special attention is given to the figure of the fiducials used for metrology. They should not be affected by polishing to guarantee for reproducible measurements during the different machining steps. Moreover, it will be shown how the optical performance of a snap-together telescope is improved by factor 2 in comparison to all diamond turned optics by using a MRF figure corrected primary mirror. Finally, another performance improvement of about factor 2 is achieved by using one mirror as a compensator minimizing residual system wavefront aberrations due to single surface deviations or mounting effects. The assembly within a few hours of a diffraction limited afocal telescope exhibiting a

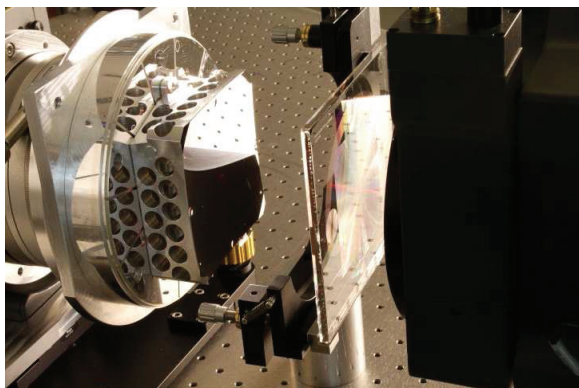


FIGURE 2. *Interferometric metrology of the primary mirror with a CGH exhibiting additional alignment features. For more detailed information see ref. [5]*

Strehl ratio of 0.95 at 633 nm wavelength will be demonstrated.

REFERENCE ELEMENTS FOR INTERFEROMETRY WITH ADVANCED CGHS

The geometry of the reference elements to be fabricated strongly depends on the metrology approach that will be used to characterize the shape of the optics. Interferometry using Computer Generated Holograms (CGH) as a diffractive null optics is one of the most precise methods for measuring the figure of aspheric or freeform shaped optics. Unfortunately, alignment of such shaped specimens in a test setup is work-intensive and may lead to the measurement of 'pseudo' figure deviations in case of an uncorrect alignment of the optics under test in the interferometric beam path.

CGHs exhibiting different alignment features can be used to effectively align the freeform under test with high precision in all six degrees of freedom [4, 5]. Therefore, alignment holograms on the same CGH are used to illuminate diamond machined reference elements in a confocal arrangement. The obtained fringe patterns are used to find the mirrors correct position in the test setup. The metrology approach fixes a unique alignment of the mirror, enabling a direct measurement of optical power and tilt of the optical surface with respect to the reference elements. The resolvable accuracy during alignment for the configuration shown in figure 2 is less than $5\text{ }\mu\text{m}$ for a lateral shift in X, Y and less than $1'$ for a rotation about X, Y and Z, respectively. The alignment accuracy for an axial shift in Z is better than $15\text{ }\mu\text{m}$.

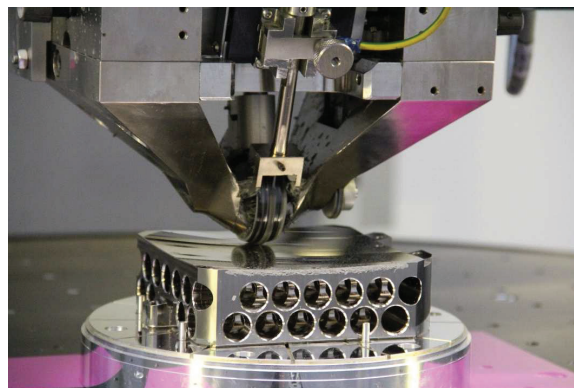


FIGURE 3. *Figure correction of the primary mirror with a 50 mm polishing wheel*

FIGURE CORRECTION WITH MRF

Diamond turning as a manufacturing approach satisfies most requirements for optical aluminum based mirrors and telescopes working in the infrared spectral region. Short wavelength applications demand for higher shape accuracies and smoother surfaces to minimize scatter losses. These goals are achievable only by applying additional manufacturing steps, like sub-aperture figuring and computer-controlled polishing techniques [6]. Electroless nickel is a state of the art polishing layer for metal mirrors working down to the extreme ultraviolet spectral range. The amorphous structure and relatively high hardness of the material offer good polishability and an achievable roughness of a few Angstroms. The mirror plating involves another diamond turning step to prepare the optics for subsequent polishing.

Following a raster path, a 50 mm polishing wheel was used to correct the measured figure deviation of the nickel plated freeform mirror (Fig. 1) with MRF. Depth, radii of curvature, and aperture sizes of the reference elements were chosen in such a way, that their shapes were almost not influenced by MRF processing. The figure deviation of the primary mirror was improved from $1.46\text{ }\mu\text{m p.v.} / 215\text{ nm rms}$ after initial diamond turning to about $45\text{ nm p.v.} / 4\text{ nm rms}$ after three iterations MRF over a clear aperture of $100 \times 100\text{ mm}$. Final figure deviation is dominated by residual mid-spatial frequency errors from diamond turning. Smoothing of such residual frequencies requires for another post polishing that works with a larger and stiffer polishing tool. In addition to the improvement in shape, the microroughness of the surface was improved from about 5 nm rms after dia-

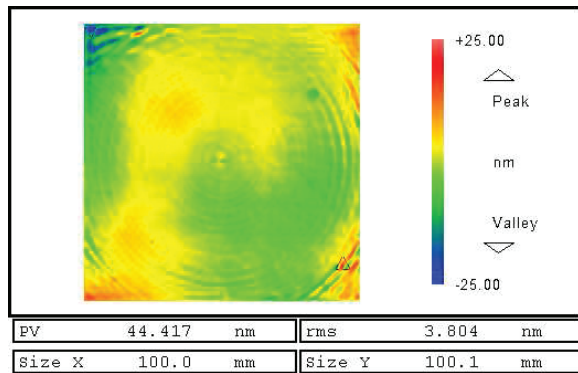


FIGURE 4. Achieved figure deviation of the primary mirror.

mond turning to less than 1 nm rms after MRF polishing. Thus, a highly deterministic fabrication chain based on diamond turning and subsequent MRF figuring can be applied for demanding metal optics working in the visual or ultraviolet spectral range.

REFERENCE ELEMENT GEOMETRY

The nominal geometries of all reference elements are generated with high precision during the diamond machining process. All subsequent manufacturing steps must maintain the shape of all manufactured references at least in a sufficient large central area in order to provide reproducible alignment during an iterative sequence of metrology and polishing. This means that according to the polishing tool used for sub-aperture figuring, a certain ratio between radius and depth of a reference element should be preferentially chosen. On the other hand, the geometry of the reference spheres must be tailored to the specific design of the CGH used for freeform metrology. The number of fringes observed for the alignment holograms, which corresponds directly to the sensitivity in adjusting the mirror under test, is primarily based on the ratio between aperture size and radius of curvature of the reference sphere. Smaller radii of curvature enhance the alignment sensitivity, but naturally decrease the aperture size and are also limited in terms of manufacturability due to the finite radius of the milling tool.

In order to investigate suitable reference geometries for metallic freeform mirrors whose application demand for a further sub-aperture figuring step, figure degradation of a broad set of 20 diamond milled concave spheres on an electroless nickel plated plano surface exhibiting different radii, apertures and depths were analyzed

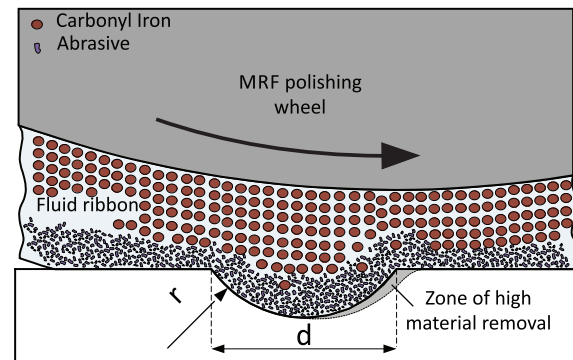


FIGURE 5. Schematic view of the polishing zone during polishing over a concave reference sphere with MRF.

after removing defined amounts of material with MRF. Figure 5 schematically shows the polishing zone during polishing over a concave reference sphere with MRF. The fluid conforms to the optical surface leading to a small zone of high material removal at the trailing edge of the reference element. This is obvious since convergence and the inclination angle, respectively, between the MR fluid ribbon and the optical surface play an important role in the nature of material removal in MRF [7]. The actual zone of material removal primarily depends on the aperture size d , radius of curvature r and the plunging depth of the optical surface in the MR fluid. All parameters directly affect the pressure distribution and shear forces along the centerline of the polishing spot, respectively. Other parameters like magnetic field strength or wheel speed have a minor impact on material removal inside of the reference structure.

Figure 6 shows the normalized rms figure error of the different diamond milled and MRF polished reference elements after discrete amounts of material removal. MRF polishing was done with a 50 mm polishing wheel and a 300 μm deep removal function. The radii of curvature of the reference elements varied in a range from 1.5 to 20 mm, which is also a suitable range for tactile or interferometric metrology according to the approach shown in figure 2. All figure errors were measured with a phase shifting interferometer and are normalized to the initially measured figure deviation after diamond milling. The evaluation of the experimental data showed that reference elements exhibiting a ratio between aperture diameter and radius of curvature larger than 0.5 were not influenced by the MRF process. This is explainable with the characteristics of the fluid flow over a dis-

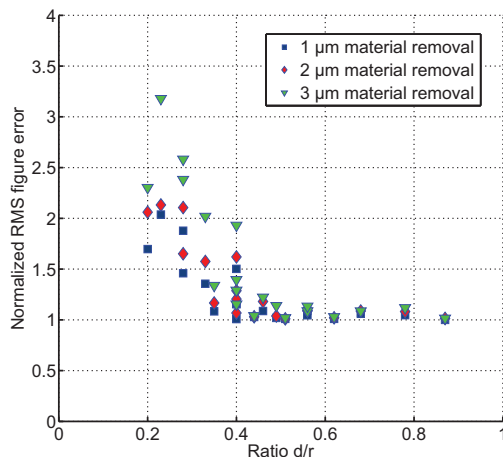


FIGURE 6. Increase of the normalized figure error of the reference elements dependent on the ratio d/r after different amounts of material removal with MRF.

continuous surface structure. The convergence of stiffened fluid ribbon and optical surface is interrupted by the reference element, leading to a drop of pressure and shear force at the leading edge of the reference element. Corresponding to the specific radius of curvature, shear forces increase approximately beginning at the center of the reference sphere. The trailing area is directly affected by MRF, having a large inclination angle and therefore increased shear forces leading to the zone of high material removal.

SNAP-TOGETHER SYSTEM ASSEMBLY

The investigated primary mirror is part of a folded Korsch-type telescope intended to image the incoming light afocally with a magnification of 4.5:1 and a field of view of 1.53 degree as a front optics for a subsequent infrared Fourier Transform spectrometer [1, 2]. A hybrid fabrication process combining diamond turning and diamond milling in a common machine setup was investigated during the last years in order to machine the optical surfaces and mounting interfaces in the same machine setup. As a result, single mirrors or mirror modules with optical surfaces having shape and position deviations in the sub-micron range from their intended design can be manufactured. Moreover, if also the telescope mounting structure is manufactured by ultra-precision turning or flycutting techniques, the whole alignment of a telescope can be achieved within a few hours only. Figure 7 illustrates the optical design of the

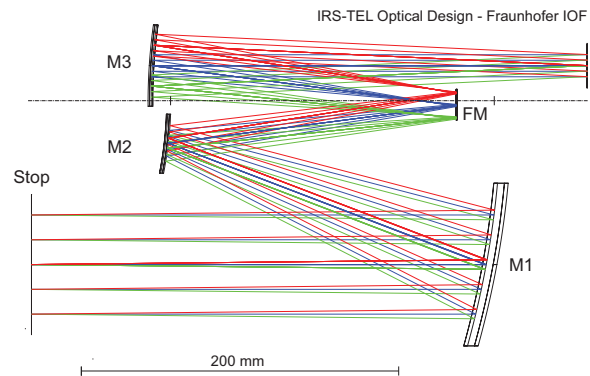


FIGURE 7. Optical design of the IRS telescope

folded Korsch telescope. It consists of three off-axis aspherical mirrors M1, M2, M3 and one fold mirror FM. All mirrors share a common axis of rotation. Even if the telescope is intended for use in the near-infrared spectral range at $1 \mu\text{m}$ wavelength, it is designed to be diffraction limited down to the short wave visible range. The transmitted wavefront error due to the optical design is limited to about 15 nm rms in the central field.

The first wavefront map shown in figure 8 illustrates the measured telescope performance with all diamond turned optical elements. Mirrors M2 and M3 were fabricated by classical diamond turning in their off-axis positions. The resulting residual shape deviations can be seen as concentric off-axis segmented rings in the final measured wavefront. In addition, final shape deviation of the on-axis diamond turned primary mirror becomes visible in form of centered concentric ring patterns [3] having the highest impact on the telescope performance. By using all diamond turned mirrors, the telescope achieves a diffraction limited performance down to a wavelength of about $1 \mu\text{m}$. A further improvement is limited by the finally achievable figure deviations of the single mirrors by diamond turning. After processing the primary mirror with MRF, it is installed in the telescope system according to the snap-together approach within a few hours only. Precisely manufactured mounting planes ensure a close placement of the optical surface to its ideal position in the optical system. The final system alignment is achieved deterministically by observing the residual astigmatism in the transmitted wavefront. Using the MRF figure corrected primary mirror (Fig. 4), a performance improvement of about 40 % is achieved. The residual transmitted wavefront aberration is mainly influenced

by the aberrations of the off-axis diamond turned mirrors M2 and M3. The Strehl ratio is improved from 0.5 to about 0.74 at 633 nm wavelength in comparison to the telescope performance with diamond turned mirrors only.

STATIC WAVEFRONT CORRECTION

After mounting and aligning a telescope system, final deviations from the ideal design wavefront will be observed in any case. Reasons for residual aberrations may be manifold and include amongst others

- Deviations of the figures and radii of curvature of single mirrors
- Misalignments in lateral, axial or rotational direction of single mirrors or modules
- Deformations due to mounting stresses or different application temperatures

The effect of single deviations from the ideal case on the system performance is usually simulated in the optical design based on the outcomes of manufacturing. Nevertheless, achieving a perfect imaging quality for a specific application may be a very complex and time-consuming process. In order to compensate for residual aberrations in a optical system, corrector elements can be introduced. They may consist of one or more refractive or reflective optical surfaces, which can also be designed as an actively controlled element. However, important for an efficient residual wavefront correction is the position of the corrector element in the optical beam path. A corrector mirror located in the pupil plane or as close as possible to this position is especially beneficial because of the equal relative ray heights in the pupil of all rays coming from different field points. The pupil stop in the telescope shown in figure 7 is located approximately 320 mm in front of the primary mirror. Because of the relatively small field of view and its relative position in the system, mirror M1 can be used effectively as a corrector element. The calculation of the necessary corrector shape is based on the measured transmitted telescope wavefront and the simulation of a corresponding higher order freeform surface superimposing the base shape of mirror M1. According to the form and the spatial extension of the wavefront to be corrected, different polynomial freeform descriptions including Zernikes, Legendre or Tchebychev may be used advantageously.

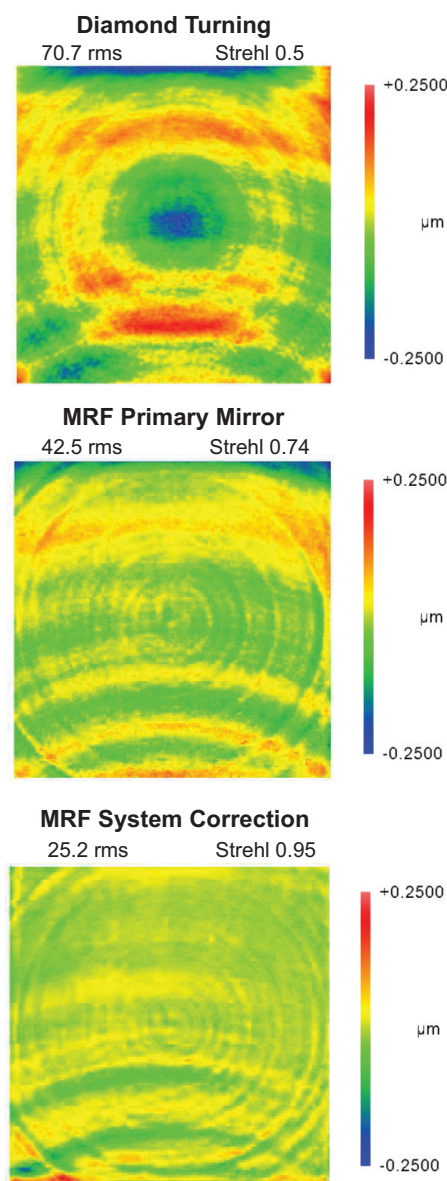


FIGURE 8. Measured transmitted telescope wavefront in single pass over a square aperture of 100 x 100 mm with the same primary mirror after different manufacturing steps. The Strehl number refers to the testing wavelength of 633 nm.

The final calculated higher order freeform is then manufactured onto the corrector element by using sub-aperture figuring techniques such as MRF [8]. The third wavefront shown in figure 8 shows the final telescope performance after figuring the primary mirror according to a simulated corrector surface. The system performance was improved to a Strehl ratio of about 0.95, closely to the theoretical limit. Most of the long-wave aberrations are compensated by the corrector surface result-

ing in a high-performance all-metal telescope with snap-together assembly within a few hours.

SUMMARY AND OUTLOOK

The application of aspheres and freeforms into optical systems require for smart metrology and manufacturing approaches. An effective production of high precision elements and its corresponding integration into an optical device is feasible only if appropriate reference elements are used. Diamond machining allows for the generation of steeply curved and complex shaped geometries and furthermore enables a fabrication of reference elements with high accuracy in terms of relative position to the optical surface. Aluminum based metal mirrors exhibiting reference structures can be manufactured with high precision by diamond turning and milling. However, highly accurate mirrors for visible wavelength applications demand for further figuring and smoothing steps. MRF was established to be a highly deterministic process for figuring nickel plated aluminum mirrors even with enlarged apertures up to several 100 mm in diameter [6].

If reference elements for metrology are incorporated into the optical surface, than their shape should not be degraded owing to the material removal in order to guarantee reproducible and high precision measurements. Concave spherical reference elements are of special interest for 3D profilometers or interferometry with appropriate designed CGHs. During polishing, the MR fluid conforms to the geometry of the reference element, leading to the generation of a zone of high material removal at the trailing edge. It could be shown that a consideration of the geometry of the reference elements during the optomechanical design helps to maintain the figure of the fiducials during MRF polishing, resulting in measurable fringe patterns during metrology. This enables a feasible production of all-metal reflective telescopes incorporating various single mirrors and a snap-together system assembly [2] for application wavelengths in the VIS spectral range.

Further improvements of a telescope performance are achieved by using one or more mirrors as optical corrector elements for residual system aberrations. The elements are fabricated as higher order freeforms according to a sophisticated design based on a high-precision measurement of the system performance. By combining ultra-precision diamond machining techniques with sub-aperture MRF figuring and snap-

together system approaches, all-metal reflective telescopes with extremely good performances can be manufactured effectively concerning time and costs.

ACKNOWLEDGMENTS

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Production Level Defect Analysis of Glossy and Freeform Surfaces

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INTRODUCTION

As manufacturing continues to make strides towards achieving success with increasingly advanced techniques allowing for the fabrication of freeform structures, the need remains to verify processes and quality of production at the end of a production line. This task can be problematic when dealing with shiny, freeform surfaces due to the lack of technologies available to evaluate these surfaces. Additionally, the sensitivity required is problematic due to the necessity that even the smallest variations in curvature or blemishes can result in a nonfunctional or cosmetically unappealing part [1]. The technologies used to attempt this inspection, often optical due to the necessity to obtain areal surface information, struggle with detection of irregularities which can contain these subtle variations in appearance. Often, after thorough analysis of available technologies, a human is chosen to perform this task due to the lack of reasonable technological solutions available. While the human has great inspection attributes, due to their ability to manipulate how the part is viewed and their flexibility to make intelligent decisions based on new features seen, they still remain subjective in their analysis. Additionally, the growing need for tighter tolerances and scrutiny against smaller irregularities put additional stress on the human visual inspector and many times this will result in irregularities slipping past the visual inspection station or overkilling the process due to a heightened level of awareness that must be present when searching for such small structures.

Given that freeform parts are reproduced in large quantities there is high need of developing processes which can ensure the quality (defect free) of each part made by replicating the

manufacturing process. Phase-shifted deflectometry, when coupled to a digital automated decision model for irregularity detection, is able to excel at irregularity detection on specular and free form surfaces while maintaining acquisition speeds that meet takt times required in facilities with very high production throughput. Phase-shifted deflectometry uses structured light illumination to derive information about a part's edge, used to check completeness and feature positioning. It also provides a measurement of the level of gloss or shininess, to check polishing processes and look for irregularities that increase or decrease surface scattering; typically resulting in aesthetic irregularities on the part. Finally, the slope of the part is observable, enabling structures that deviate from the nominal spline of a surface to be detected. With this information, one is able to derive the local curvature and even height of localized structures on the surface to help aid in irregularity classification. Such information provides an ability to rapidly monitor the process as well as ensure quality of the components [2].

PRINCIPLE OF OPERATION

Phase-shifted deflectometry acts on the principle that one is observing the projection of a light pattern on the surface of a test piece instead of focusing on the surface of the part itself. By viewing the pattern, the observer is able to see alternate fringes or sections of the projected pattern while effectively removing the part from the optical system. This differs greatly from technologies such as fringe pattern triangulation which rely on scattering from the surface to obtain depth changes between lateral measurement points and cannot directly view the slope of the measured region but instead

have to derive such information. Additionally, by widening the view to the projected pattern, rather than focusing on the measurement point, the system is able to record this detail over a much larger area and perform these observations more quickly.

Phase-shifted deflectometry allows for a wide range of surfaces to be evaluated with the main advantage occurring on highly specular or shiny surfaces. This is generally not the case for optical systems which typically have increased difficulty obtaining images as the part becomes more specular, as well as for conventional deflectometry which requires highly specular surfaces (FIGURE 1).

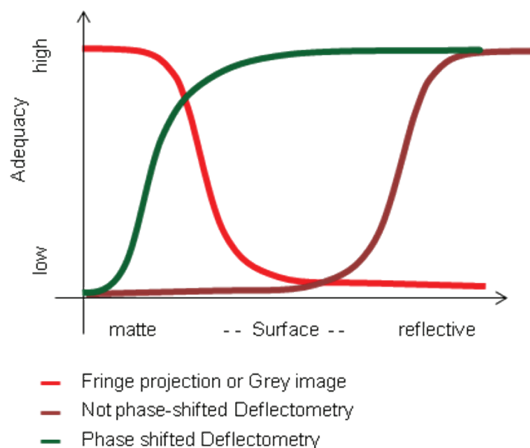


FIGURE 1. *Phase-shifted deflectometry adequately evaluates both specular and semi-specular surfaces.*

The phase-shifted deflectometry system adapted for visual inspection consists of a white light LED array that generates the continuous illumination patterns which are used to generate the primary images used for defect analysis. Computer numerically controlled positioning stages which move the part under test into optimum fields of view. Various cameras, line and matrix sensor, whose optical resolutions are matched to the limits of the defect sizes being looked for. Finally, a high performance computing cluster is added and scaled accordingly to allow for any evaluation algorithms to be performed at the speed of image acquisition such that evaluation times

meet the strict cycle times during production and 100% inspection can be achieved.

The system's white light source produces a series of sinusoidal fringe patterns that are viewed by the cameras as a reflection from the object under test (FIGURE 2). The series of patterns produced by the illumination element are shifted in phase from each other and recorded by the system.

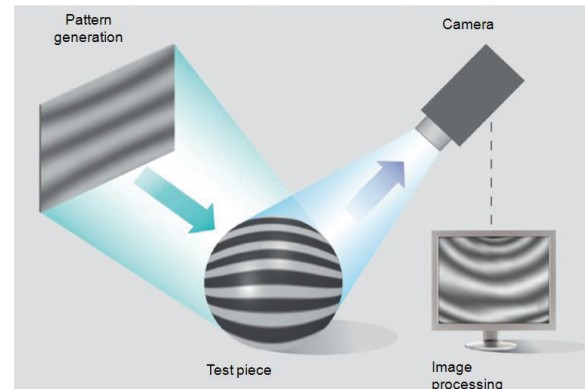


FIGURE 2. *Diagram of the phase deflectometry setup.*

The resulting images that are acquired are then used to derive three primary images for image analysis; a synthetic grey image, a gloss or shininess image, and a phase or inclination image.

METHODS

The resulting primary images are used to objectify features on the part for inspection tasks such as completeness, position or more importantly, detection of surface irregularities (FIGURE 3) [3].

The first (FIGURE 3a) channel; shows a grey scale image of a polished chrome-coated plastic letter 'Z' under patterned illumination. This is similar to the grey scale images seen from typical vision systems; an image under fixed illumination is viewed from a single vantage point. A large majority of the time the surface defects that are present are very difficult to notice if the surface is not observed from a particular angle. This can result in slippage of a defect; additionally, when the visual inspector mimics this process and is not repeatable in the

angles, or vantage points, in which they view the part it is very easy to miss such an irregularity further adding to the subjectivity of the “good or bad” decision process.

A second (FIGURE 3b) channel; the synthetic grey scale image, is a low noise image created by the synthesis of multiple phase-shifted images. This image has homogeneous illumination which is widely independent from the shape of the part being tested. This image can be compared to imaging systems which use multiple vantage points to attempt to image irregularities and is a highly useful channel for functions like edge detection, variations in color, optical character recognition and other discrete edge finding characterizations.

A third channel (FIGURE 3c); the gloss image, helps to highlight areas of increased light scattering which are independent of the shape of the part under test. This image gives the distinction of gloss and brightness which is critical for proper detection of cosmetic irregularities, or irregularities which exhibit an increase or decrease in scattering from the nominal surface. In this case the very superficial scratches that change the part’s cosmetic appeal are noticeable. This channel is also very useful for tracking changes in polish quality or objectively viewing the surface for any dull spots.

Finally (FIGURE 3d); the inclination image can be obtained from the phase information and used to determine any deviation in the nominal slope of the part. This image can be highly critical to the evaluation of a functional surface and is necessary when detecting surface irregularities which cause a deviation from the intended spline of the part such as scratches or dents. Additionally, the curvature of the part or any noticed irregularities can be derived from this image providing further analysis of the type of irregularities observed on the surface. These irregularities include indentations, pimples, or other negative and positive irregularities. In this case a linear shaped dent which influences functionality is noticed on the part; this

irregularity is very difficult to see in the previous channels due to the specular nature of the dent which resembles characteristics off the surrounding surface finish unlike a typical scratch that has a discontinuity in scattering characteristics from the surrounding surface.

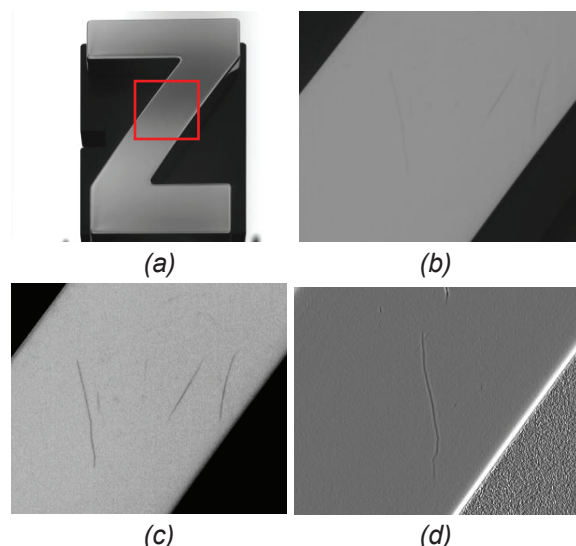


FIGURE 3. a) One grey scale image taken during one of the multiple phase shifts. b) Synthetic grey image showing a homogenous illumination of the test part. c) Gloss image showing areas which deviate from the nominal gloss level. d) Inclination image showing deviations in the nominal slope of the surface.

While image acquisition and lighting conditions remain a critical component in the system’s robustness a vital piece of the puzzle is the software which drives the system. Often times it is the digital decision process, not the quality of imaging that results in poor performance or unseen irregularities.

RESULTS

Earlier works have introduced phase shifted deflectometry as a tool for surface analysis at high speed with a focus on the ability to observe defects difficult to observe with classical optical methods. Phase-shifted deflectometry has also been shown to provide high quality quantitative analysis when careful considerations have been made about part geometry or system design. Both of these important aspects aside, phase-shifted deflectometry can be used to repeatedly

inspect freeform surfaces as a comparison tool to objectify changes from a nominal, or 'golden', part. Defining irregularities, by the characteristics the human visual inspector uses to observe them, it is possible to drive software evaluations to locate the same irregularities. After the irregularities are identified, their severity can be assessed objectively, ranked to objectify the visual inspection process, and then repeated without the need to quantify such irregularities. While quantification of irregularities remains important, many industries do not need to perform dimensional analysis of each irregular aspect of a part and the simple knowledge that a process is shifting towards an undesirable result is enough information to help control that process.

Many manufacturing processes create parts whose irregularities only affect the cosmetic nature of the part in question. It is often difficult to objectify these irregularities as defects when measuring these parts. A common method of measurement is to take surface profiles from a profilometer; however, it is a common complaint that parts which contain the same surface profile reading from a profilometer have different optical characteristics. Consider these polished metal parts (Figure 4) whose functionality is independent of the finish of their surface. However, for the manufacturer, the cosmetic design of these parts is very important. By generating a channel which is able to view how much the surface scattering of the part is affected by the process one can now objectively come to a conclusion on whether or not a part meets the cosmetic criteria that was once trying to be evaluated by taking surface profiles.

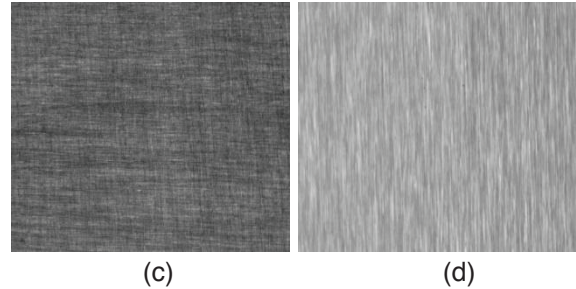
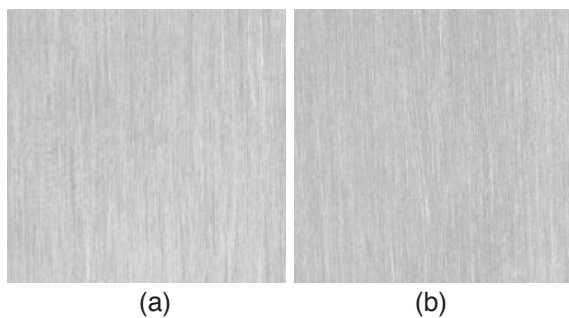


Figure 4. a,b) Grey scale image of finished surfaces with similar roughness profiles of a highly reflective spherical surface. c,d) Gloss Images of the same surfaces showing a distinct difference in optical characteristics. This difference allows the inspector to now differentiate the two surfaces objectively.

Conversely, many industries require analysis of their freeform surface to determine if irregularities that are occurring are more or less severe than their smallest allowable irregularity. Often it is difficult to quantify these irregularities as the 'depth from the nominal surface' or 'areal size' is not the significant characteristic. Often, when evaluating these parts, visual inspectors are given sample images or sample parts to use as the determining factor. This practice can lead to very subjective analysis and inconsistent inspection results between inspectors. Consider the metallic part with two irregularities (FIGURE 5). While both irregularities show deviation from the nominal surface, the irregularity on the right is much more visible cosmetically due to the sharp transition near its edges. These values can be considered with phase-shifted deflectometry and the irregularities' scores can then be tied to this analysis. This gives the production team an easier method for determining if the irregularity has gone from a negligible cosmetic irregularity to a defect which affects the functional surface of the final part.

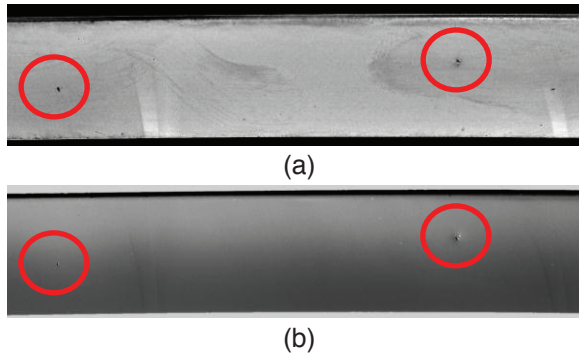


Figure 5. a) Gloss Image showing the cosmetic view of the irregularities. Each irregularity is visible to the inspector however it is difficult to objectively determine the severity of each. b) Inclination image showing how each irregularity deviates from the nominal slope. With this information, an evaluation can be made which identifies the irregularity on the right as the more severe.

One may assume that once an irregularity shifts from a cosmetic type to a more functional type that greater scrutiny would be necessary to determine if the irregularity has passed a certain threshold. Often process engineers have difficulty determining which metric to use when measuring these irregularities. And usually it is the case that an internal standard becomes accepted even though these methods could be seen from an outside observer as highly ineffective. Take for instance a gauge pin, or worse, a common household item that is used to probe the surface of a part. This is one method seen all too frequently when trying to probe highly reflective free-form surfaces for irregularities which may result in functional failure of a device. Figure 6 shows a highly reflective metal surface which is designed to mate with a counterpart, in this instance any irregularity which disrupts the mating surface should be identified as an irregularity however with the aforementioned method it is common to miss parts whose irregularities have low frequency transitions at the edge of the irregularity. With phase-shifted deflectometry this situation can be avoided and it is possible to reduce the amount of parts which slip past the inspector.

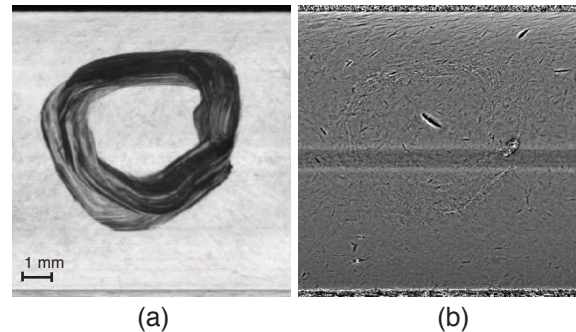


Figure 6 a) Grey scale image of a highly reflective spherical surface. b) Inclination image showing severe irregularity and irregularity in the spherical shape.

CONCLUSION.

Phase-shifted deflectometry can be used to objectify the visual inspection process on mass produced free-form surfaces. When used as a comparison tool for detection of irregularities that deviate from a nominal part shape it is possible to work towards achieving better process control within a manufacturing line. This allows a manufacturer be confident that his production has minimal overkill and slippage. The ability to maintain low cycle times and keep up with high throughput demand for 100% inspection makes phase-shifted deflectometry the next logical solution for enhancing the visual inspection process.

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In Situ Metrology for the Manufacturing of Large Segmented Telescope Mirrors

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Introduction

The Thirty Meter Telescope (TMT) is the largest telescope slated for construction in 2014. It is based on 492 large segmented off axis 1.45m mirrors resulting in a telescope diameter of 30m with a 20 arc minute field of view. It is designed for observations from the near-ultraviolet to the mid-infrared. By design, it complements the scientific capabilities of the James Webb Space Telescope and Atacama Large Millimeter Array. The manufacturing of these mirrors has to be carried out over a timeframe of 2-3 years. This will require a very efficient polishing and metrology process. In a previous paper [1] we described the stress mirror polishing (SMP) approach to manufacturing these segments.

SMP starts with a plano or spherical form (curvature constant across the surface). A bending fixture applies forces and moments in a controlled manner, causing the surface to assume the inverse of the form desired. The assumed form does not need to be axis symmetric, and in the case of TMT, none are axis symmetric. A large tool efficiently removes material from the intended surface. Since volumetric removal rates are proportional to tool area, the method assumes most rapid finishing. Once the mirror has been optically finished to the process goal while held in the bending fixture, it is released from the fixture and will elastically assume the desired form. [2, 3]

Metrology Scheme

Typically during grind and polish of telescope mirrors they are measured using coordinate measuring machines for the low order surface shapes. This is however too time intensive. Therefore an insitu measurement device is needed.

The follow on finishing step after stress mirror polishing is ion beam finishing of the segments. The capture frequency of this process determines the required spatial measurement uncertainty of the metrology instrument. This is captured by a power spectral density plot [PSD] versus the spatial frequency.

Insitu Metrology Approach

To support the rapid polishing of the off axis non-symmetric segments an in situ metrology system was designed, that can measure the part surface shape with respect to a reference sphere. It consists of a carbon fiber panel [CFP] that contains 61 touch probes to measure the shape of the mirror. The CFP is suspended by flexible cables to avoid introduction of external forces into the panel. The profilometer [Sag Array] can be rotated on its support structure to increase the number of measured points and decrease noise print through.

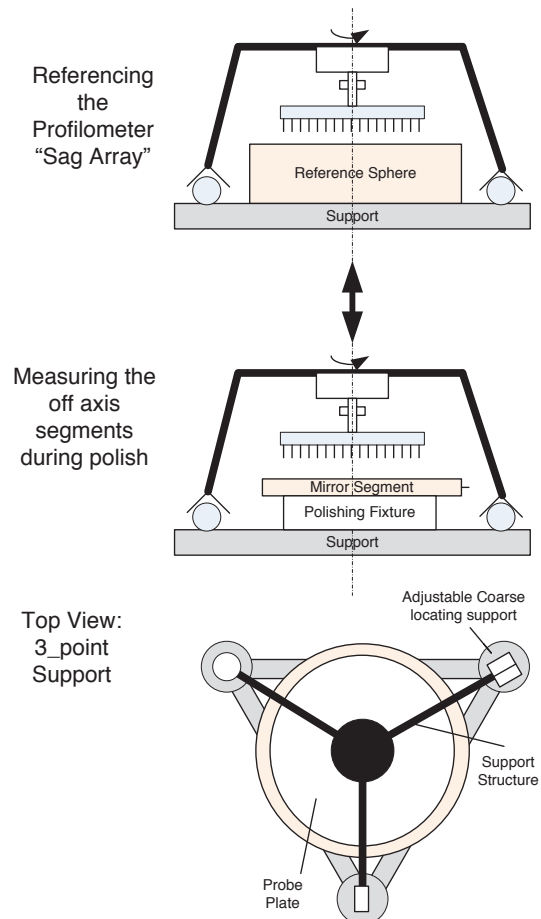


FIGURE 1. In situ measurement process with a reference sphere.

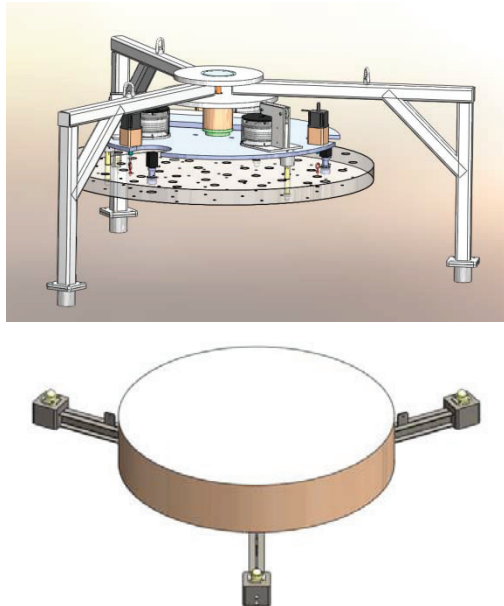


FIGURE 2. Solid model of the profilometer and the reference sphere with its docking station.

Error Budget for the Profilometer

Basis for the design is an error budget that splits the overall measurement error into its individual components.

The overall measurement error target was 1um peak to valley.

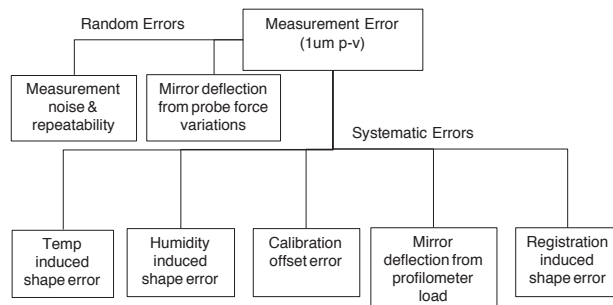


FIGURE 3. Error budget.

The measurement noise of an optical surface is a function of the measurement location and its associated noise [4]. The resulting noise gain is

$$\left(\frac{\sigma_{\phi}}{\sigma_n}\right) = \{TR [(G^T G)^{-1}]\}^{1/2}$$

where TR(M) is the trace of a square matrix M and G is the matrix of Zernike polynomial values at the profilometer touch probe positions

$$G_{i,mn} = z_{nm}\left(\frac{p_i}{R}\right)$$

This allows the computation of the Zernike estimated noise versus the maximum Zernike radial order. Than one can determine at what spatial frequency the instrument noise will dominate.

The mirror deflection based on probe forces can be estimated using FEA analyses. The probe errors contain a systematic component which is displacement dependent. However the probes do have a residual random force error. Temperature and humidity were also modeled using FEA. The moisture input data was local humidity data which causes the panel to expand due to the hygroscopic of the resin and actual factory temperature logs. Axial temperature gradients in the room cause dominant power errors as illustrated in figure 4.

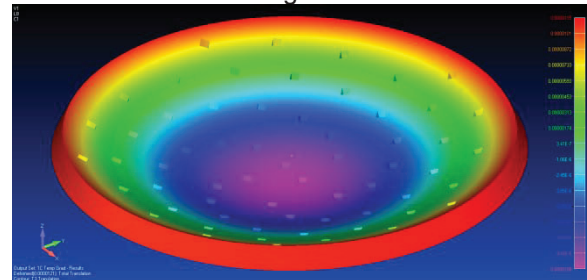


FIGURE 4. Effect of axial temperature gradient on the CFP panel.

Touch Probe Pattern

The touch probes were laid out in a spiral pattern such that they would not overlap when rotating the sag array 0, 120 and 240-Degrees. The probes were spaced along the spiral such that each probe was covering an equal area. The actual location of the touch probes is measured upon assembly of the probes in the CFP.

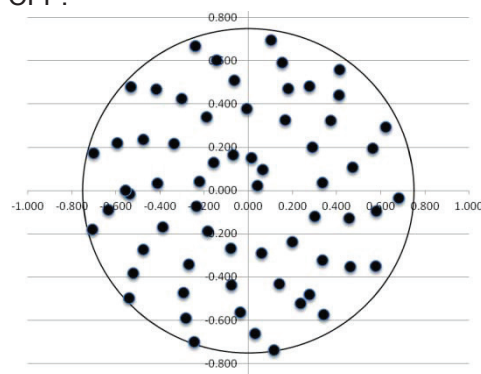


FIGURE 5. Spiral probe pattern.

Mechanical Profilometer Design Factors

Mechanical criteria for the profilometer design are:

- Stable measurement
- Prevention of fracture due to docking (Impulse)
- Prevention of fracture due to hertzian contact (Weight)
- Prevention of deformation of the blank due to the weight of the CFP

In figure 6 the CFP panel is suspended at three load points from which it is lowered on the mirror surface. Three tooling balls make contact with the mirror surface. The radius of the balls has to be large enough to prevent fracture of the mirror glass surface.

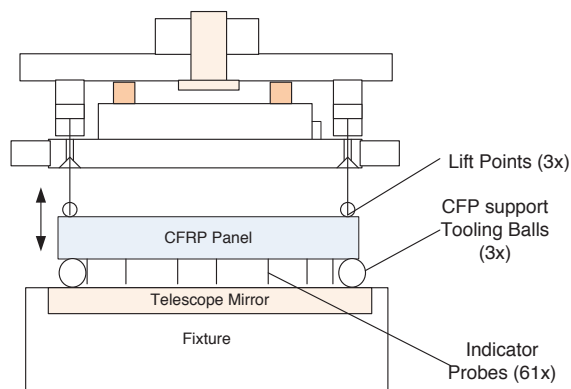


FIGURE 6. Simplified view of the CFP support.

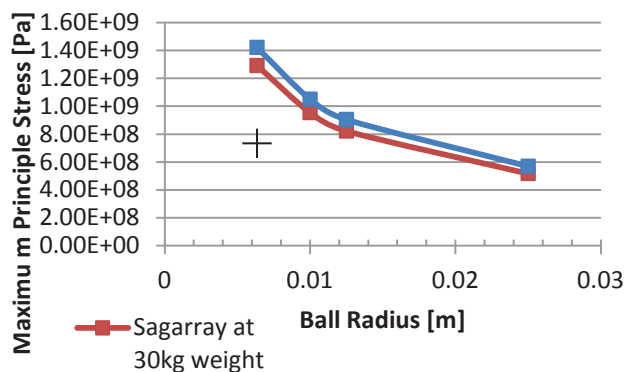


FIGURE 7. Hertzian ball contact.

During measurement the mirror rests on hydraulic pads. The weight of the CFP panel is transferred by three ball-point loads onto the mirror surface. This generates shape print-through from the support pads which has been calculated via FEA.

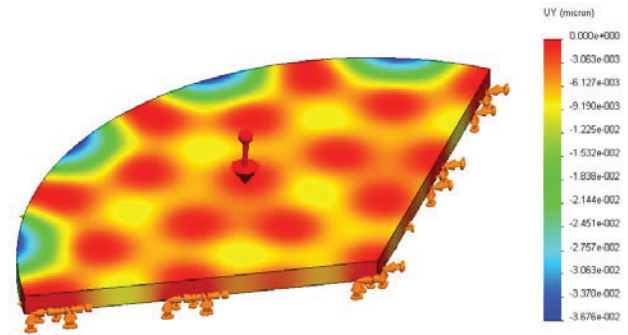


FIGURE 8. Print-through generated by the CFP panel loads on the surface of a Clearceram mirror.

Reduction of Weight Print-Through

In order to reduce the print-through due to the load exerted by the CFP panel, a counterweight mechanism was designed according to figure 9. This allows the load to be optimized, and the weight constrain on the CFP panel to be loosened.

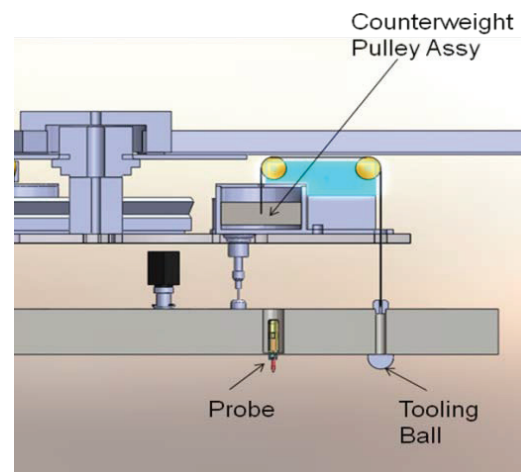


FIGURE 9. Counterweight mechanism for offloading the CFP panel.

Registration of the Instrument

The level of allowable decentration of the profilometer versus the optic has to be known in terms of surface error. The resulting surface error depends on the type of mirror segment. The decenter was synthesized for the inner and outermost segment. The resulting surface error for 1mm decenter is less than 30nm peak to valley (figure 10, 11). This type of profilometer centration is easily achieved by using a 3-point mount as docking mechanism.

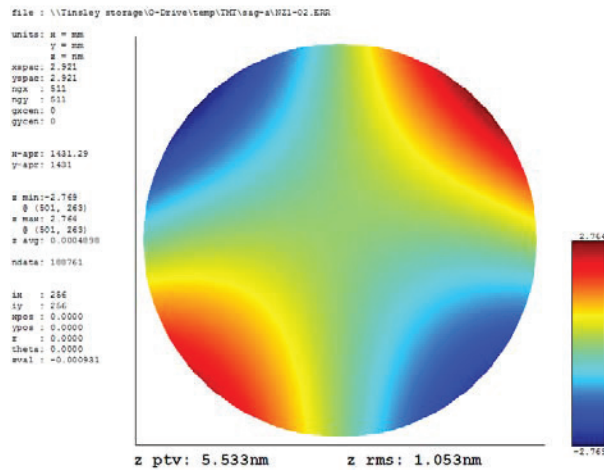


FIGURE 10. Shape from 1mm decenter of the innermost segment after removing piston and wedge.

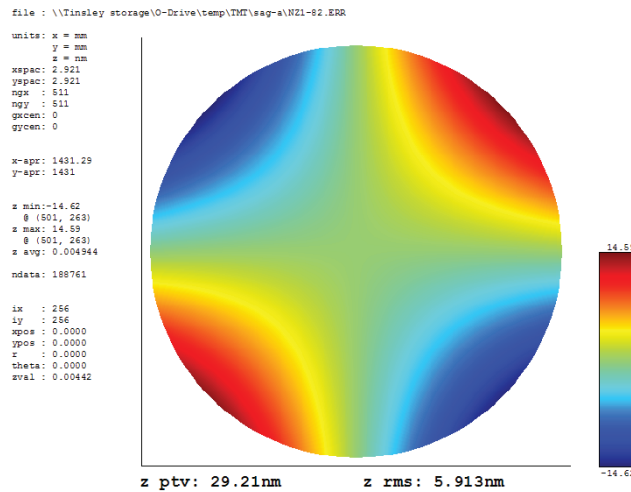


FIGURE 11. Shape from 1mm decenter of the outermost segment after removing piston and wedge.

Conclusion

In conjunction with the previous work on stress mirror polishing [1] the built of this insitu profilometer completes the large segmented mirror workcell at L-3 IOS, Richmond, CA.

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FREEFORM MEASUREMENTS WITH TACTILE AND OPTICAL SENSORS ON A CYLINDER COORDINATE MEASURING INSTRUMENT

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ABSTRACT

A new high accuracy cylinder coordinate measuring instrument can be equipped with both a tactile probe and an optical point sensor. In addition to the measurement of aspheric samples the system should also be capable to measure free form samples. Measurements with the tactile and optical sensor applying both sensors are presented. The results reveal the performance of this system and the two sensor types to measure off-axis aspheres and free form samples.

INTRODUCTION / SYSTEM DESCRIPTION

Using a tactile point sensor for 3D measurements of aspheric lenses a new metrology approach was presented in [1]. The system consists of a profilometer combined with a high precision rotational measuring axis from a so-called formtester. This system allows to collect linear scans across the sample and polar circles around the rotational symmetric sample. This grid of data points describes the 3D surface of the sample. In principle the system was also capable to measure freeform shapes such as cylinders. First results for were shown in [2, 3]. In order to improve the accuracy of such a setup tests with a specific high precision formtester showed promising results. This formtester is a high accuracy cylinder coordinate measuring machine with two linear measuring axes and one rotational measuring axis (Fig.1). The system is a very stable mechanical setup consisting of high precision axes and an internal compensation system (Fig.2). With this compensation system the measurement values are corrected for any nonsystematic movements of the axis, e.g. due to load changes or a nonrecurrent behavior of the guidings. Included is also a temperature compensation with a local monitoring system. The system was originally designed for applications in mechanical engineering and automotive industries. As the measuring process for lenses is different to these applications the system was optimized considering the new requirements.

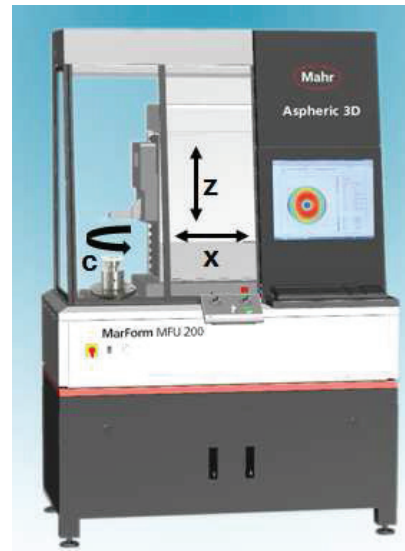


FIGURE 1. Cylinder coordinate measuring instrument MarForm MFU200.

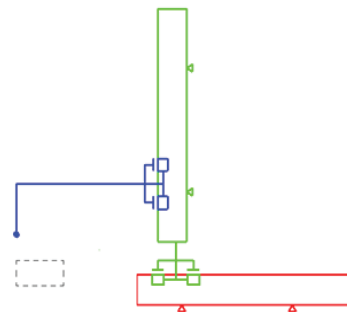


FIGURE 2. Principle of compensation system.

A unique feature is that the system can be equipped with both a tactile and a new type of optical point sensor, thus allowing to directly compare tactile and optical measurements on one system. As optical probe sensor a setup based on white light interferometry has been developed for this special application. The goal was to obtain a high resolution sensor with a real time data processing for tracking processes,

i.e. by getting the height values from the probe the system can follow the contour of the sample. In addition the probe system should have a small, lightweight and exchangeable probe, i.e. a small optical system with a fiber connection. The probes can be exchanged via a magnetic contact allowing to switch in between different probes. In Fig.3 is shown a tactile probe arm, an optical probe and a probe arm with both an optical probe and tactile probe tip.

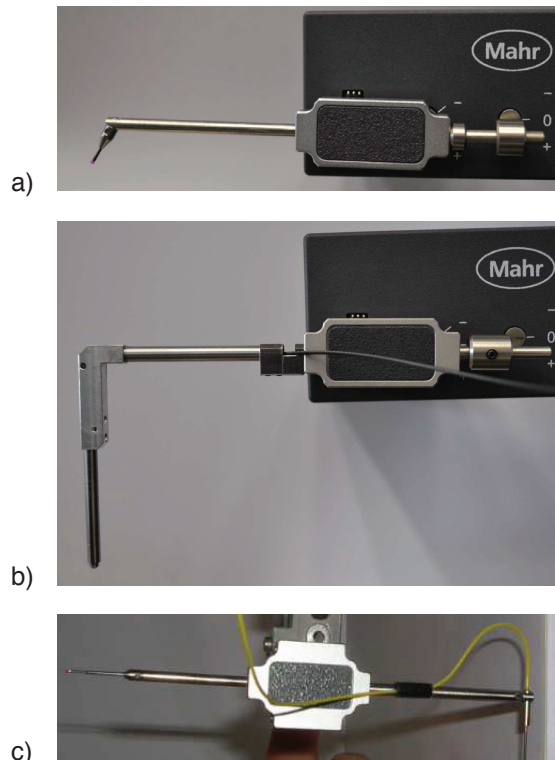


FIGURE 3. a) Probe arm with tactile probe tip
b) Optical probe
c) Probe arm with tactile tip and optical probe

As it is desirable to measure optical components without contact the optical sensor is usually favored. However, the optical sensor has other limitations, e.g. the slope of the surface, than the tactile sensor, thus the application of both sensors increases the range applications. Performing the same measurement task with both sensors allows to compare the performance of the two sensors. Due to its three high precision and well controlled measuring axis the system is not limited to rotational symmetric samples, i.e. with the combined movement of the rotational and the two linear axis also freeforms can be measured.

Considering the measuring strategy freeforms may be categorized in off-axis aspheres, cylinder and toroidal shapes and others. As the instrumental setup is designed for rotational symmetric aspheric lenses, circular objects can be measured with the standard scanning paths over the surface. However, the instrument is capable to lift off the probe tip and contact again automatically. Thus even segments of circles and lines can be collected allowing to describe e.g. a rectangular geometry.

The in the following presented measurements were performed in the way that the measuring system does not have any information about the sample under test. The 3D data are collected by a set of circular scans on different x-positions. The scans are performed in the tracking mode where the c-axis is rotated and the z-axis automatically tries to keep the probe system within its measuring range. The obtained 3D-data points are interpolated to create a topography with an even x-y grid for subsequent analysis and visualization. In the analyzing process the nominal shape of the sample is fit into the measured data and a differential topography is created.

OFF-AXIS APSHERES

Applying the cylinder coordinate instrument to measure off-axis aspheres, two different approaches can be described. One approach for off-axis aspheres is to center the aspheric sample on the rotational axis. This can easily be performed with help of the automatic centering and tilting table on top of the rotational axis included in the system. However, if the sample only consist of an outer segment of an asphere it will not be possible to center the sample. The measuring process itself is in principle the same as for rotational symmetric objects. One difference is that for the outer circles the probe does not detect a surface on a part of its circular trace.

The other approach is to consider the off-axis asphere as a freeform. Without any centering the three-dimensional data are collected by turning the sample and following the surface with the probe by moving the linear axis. A following analyzing process will fit the measured data into the nominal shape or relates the data to the mounting device or reference marks which can be measured too.

Considering the asphere as a freeform the following measurements were performed. The sample is a standard centered asphere of about 50mm diameter. The asphere is measured

centered to the rotational c-axis of the measuring instrument and then, in order to simulate an off-axis asphere, decentered in steps. The circles in Fig. 4 indicate the measured areas. The smallest area does not include the center of the asphere.

In Fig. 5 are shown first measuring results with the optical probe tip, i.e. the differential profiles after subtracting the nominal aspheric shape. The profile at the top shows the result of the centered profile with a PV of about $1.3\mu\text{m}$. The profiles below show the results of the decentered asphere. Comparing the results two problems occurred. Although the color scale is the same for the profiles the height level is different. In addition the fitting process introduces different tilts in the off-centered asphere. However, in general we obtain a similar topography with a similar PV for the measured areas. More details can be seen in Fig. 6 where a linear profile from the centered asphere and an extracted linear profile from the 3D measurement of an off-centered asphere are shown. The differences can be explained by some tilt and a little radius deviation.

This was a first test to prove if the system is capable to perform this kind of measurement. Further optimization and additional tests on different sample are planned to show the accuracy limits.

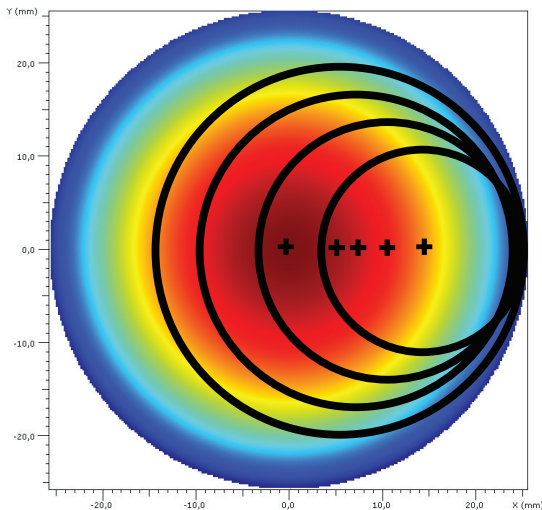


FIGURE 4. Measured asphere, circles indicate the measured areas.

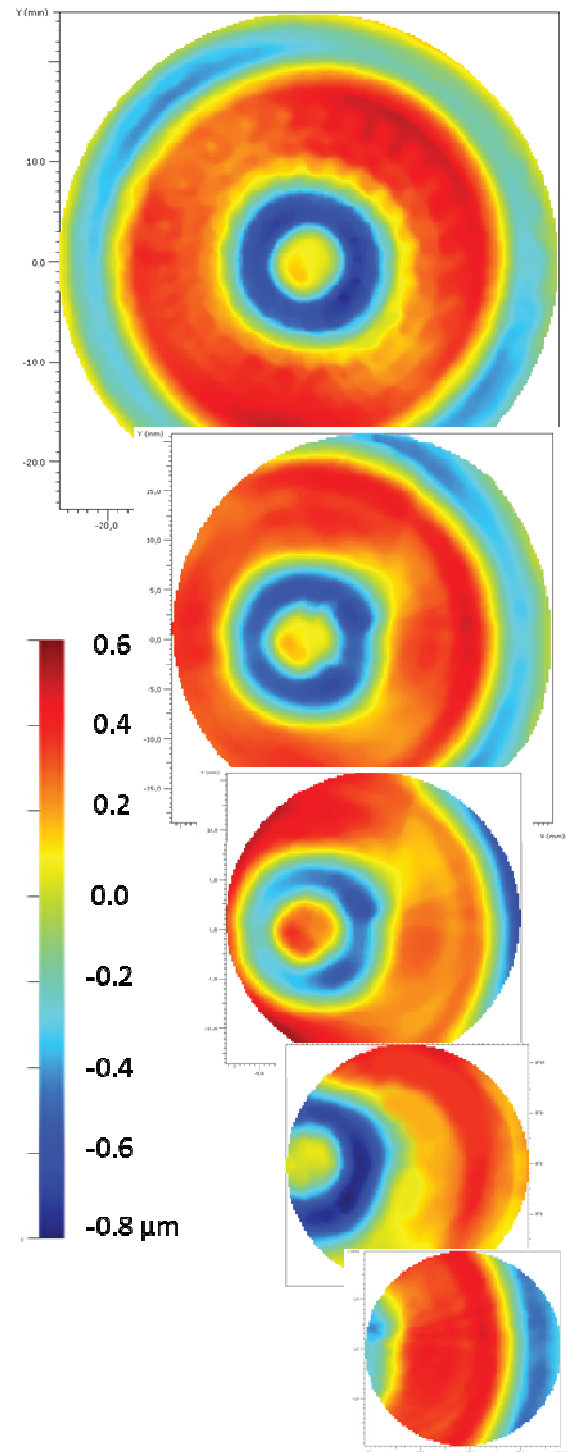


FIGURE 5. Differential profiles of the measured of the centered and decentered asphere.

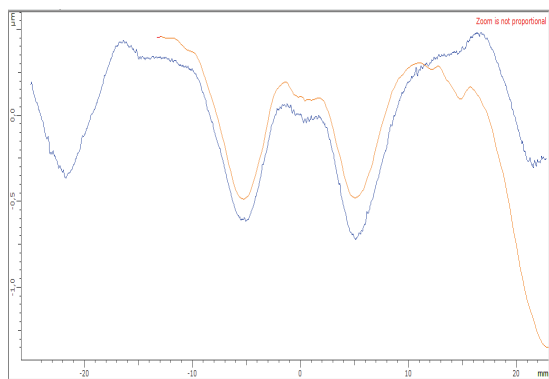


FIGURE 6 Linear profile from the centered asphere (blue) and an extracted linear profile from the 3D measurement of the off-centered asphere (red).

A limitation independent of the measuring data is the fitting process. Depending on the shape of the asphere and the measured area the applied fitting process was not stable. In particular, for the measurements not including the center, the fitting process often failed. The fitting routine was designed for nearly centered samples, so a further development may lead to a stabilization of the fitting process. However, depending on the shape of the asphere and the off-center area there will be limitations.

ZYLINDER LENSES

For the measurement of cylinders and toroidal shapes it is also useful to center the samples in order to be able to collect two linear profiles describing the two base contours (line and circle for the cylinder) independent of the 3D Data. The 3D-topography is then measured considering a freeform by a set of polar traces. For e.g. rectangular outer shapes these circles may also be interrupted. In Fig. 7 are shown the measuring results, i.e. the 3D data (a) and the differential topography (b), of a measurement with the tactile probe of a cylinder lens with about 20mm radius and an x-y extension of about 10x20mm. For this measurement with the cylinder orientated parallel to the x-axis we get a PV of about $1.8\mu\text{m}$.

In order to determine, if the orientation of the cylinder has any influence on the results, the sample is rotated. Fig. 8 shows an example of a measurement with the sample rotated by 45° . The color scale in Fig. 7b), 8 (and 9) is the same. In this case we obtain a very similar topography with a similar PV value compared to the previous measurement.

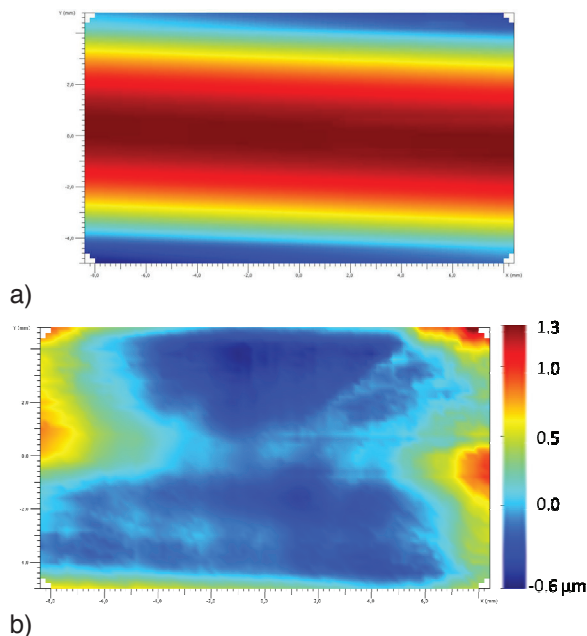


FIGURE 7. Measurement of a cylinder lens with the tactile probe. a) 3D data, b) differential topography.

For comparison measurements of the same sample were performed with the optical sensor in addition to the tactile one. An example of the non-contact measurement is presented in Fig. 9. Again a very similar topography and PV-value is obtained, thus no difference between the optical and tactile probe system could be detected in the results.

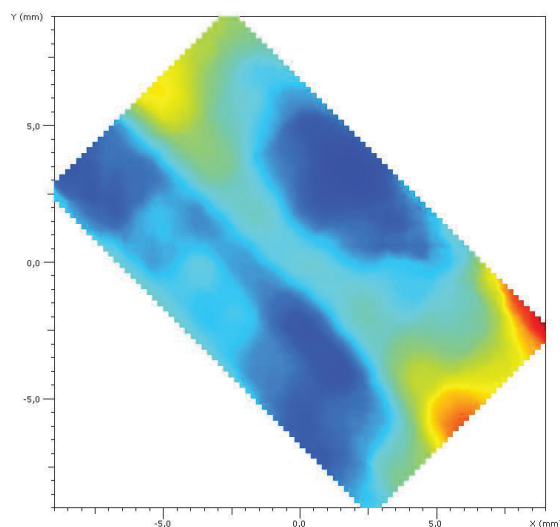


FIGURE 8. Measurement with the tactile probe of a cylinder lens rotated by 45° .

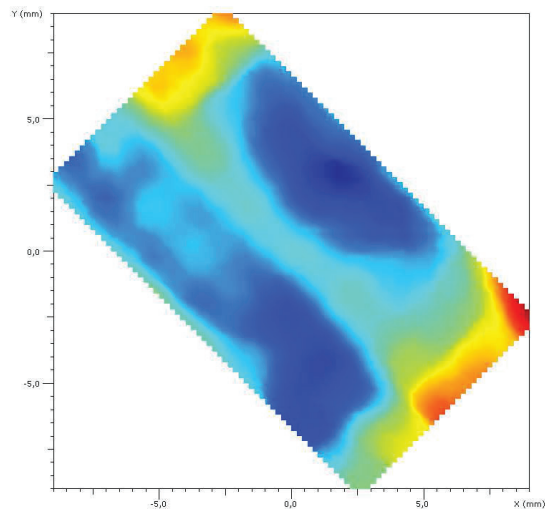


FIGURE 9. Measurement with the optical sensor of a cylinder lens rotated by 45°.

CONCLUSIONS

The presented results are obtained from first measurements, with this new measuring system in a freeform mode, i.e. the sample can be a non-rotational symmetric shape. The results show that the system can in principle perform the measurements.

Comparing the tactile and optical measurements no differences concerning the accuracy could be detected for these measurements. Differences can be found in the practical use. The tactile probe system can cope with higher slopes of the surface compared to the optical sensor due to limitations in numerical aperture. On the other hand the tactile probe has limitations in the mechanical dynamics, i.e. measurements with the optical system can usually be performed faster.

A detailed analysis of the raw 3D-data and the final results show that the major limitations for the accuracy are the tracking mode and the data processing. The measuring system has not been used in this kind of tracking mode before. First test show that a further optimization of the tracking control and its parameters has some potential for improvements. In particular for the off-center aspheric measurements limitations of the fitting process and artefacts of the interpolation routine are found. As the applied data processing was in principle designed for rotational symmetric samples there is much scope for improvements.

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INTERACTIONS BETWEEN MANUFACTURE AND MEASUREMENT OF OFF-AXIS ASPHERES

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INTRODUCTION

In this paper we reflect on our experience at the National Facility for Ultra Precision Surfaces in North Wales, manufacturing 1.4 m hexagonal, off-axis mirror segments, as prototypes for the European Extremely Large Telescope. The telescope optical configuration assumed for the work herein reported, was the original 42 m aperture design [1] (subsequently de-scoped by ESO to 39m).

The prototype E-ELT segments are particularly challenging in regard to the long radius of curvature (84 m +/- 200 mm), off-axis aspheric form, edges, and the requirement to *match* segments in terms of base-radius and conic constant. Any surface-error consequent upon mis-match, simply constitutes a term in the overall error-budget required to meet the form specification (worst segment:- 50 nm RMS surface; average segment:- 25 nm RMS surface). The first segment ROC can therefore drift within the stated +/-200 mm tolerance, but subsequent segments must then match the first one. We discuss how this has been achieved, by directly grinding the base off-axis asphere into the hexagons using the Cranfield University BoX machine [2], and then polishing on a 1.6m Zeeko CNC machine at the National Facility in North Wales.

We have also identified some interesting parallels between aspheric mis-fit of surface and tool on one side, and effects of dynamic range of an interferometer on the other. The ability to perform corrective processing of grey parts is economically attractive but presents issues for metrology. We also discuss how the

requirements for the support system for the part are different for metrology and fabrication, and its resolution.

ASPHERIC MISFIT

Grinding and polishing

The segment process starts with BoX-grinding of the off-axis asphere [2], which for the peripheral segments are ~200 μm aspheric with respect to the sphere best-fit to the specific segment. The part is then processed using a combination of bonnet-polishing and hard-tool smoothing, both with re-circulated cerium oxide slurry. Aspheric misfit is not an issue for bonnet-polishing, as the bonnet naturally molds around the local asphere.

However, with hard (e.g. pitch) tools, the misfit as the tool traverses over the asphere limits the maximum practical tool-size. This is exacerbated if tool-rotation is used to achieve adequate removal-rate, because the tool, perforce, must itself be rotationally-symmetric. The tool is pressed on the part at the start of the raster tool-path, and then misfit increases along the path. We have previously considered misfit quantitatively in some detail [3], and shown that the maximum for a 100 mm diameter rotating hard tool, traversing over an outer E-ELT segment, is ~800 nm. The corresponding figure for a 150 mm tool is ~1.6 μm .

When polishing segments, empirical evidence indicates that a 150 mm pitch tool starts to leave mid-spatial frequency artefacts, whereas a 100 mm one does not. We interpret this as corresponding to the predominantly 1-2 μm particle size of the Cerox Super 1663 cerium oxide used [4]. We contend that the particles act

as a buffer, when particle-size exceeds misfit. This has the implication that larger tools could be used, providing that the abrasive particle size is increased in proportion to the misfit. C5 and C9 (5 μm and 9 μm) aluminium oxide are obvious candidates.

Interferometry

There are parallels with interferometry, and in particular, our use of a sub-aperture non-nulling interferometer for:-

- measuring regions overlapping edges, at the higher spatial-resolutions needed to resolve details of edge-profiles in process-diagnostics
- infilling missing data in full-aperture nulling interferometry (due to CGH artefacts, and a small central obstruction in the optical test)

Re-focusing a sub-aperture interferometer at a given location on the surface of a segment bears similarities to pressing a hard, rotating tool at that location as described above: tool-rotation forces the its profile to be axially-symmetric. Interferometer departure from null is given by the difference between the spherical reference wavefront, and the aspheric test wavefront, as the interferometer tracks across the surface. The limiting factor is then the *slope* of the misfit over the sub-aperture, due to finite pixel-sampling, optical transfer and phase ambiguities.

We conduct stitching interferometry in-situ on the 1600 Zeeko machine. This uses a dioptic beam-expander with a clear aperture of 180 mm, feeding a 4D Technologies 6000 compact simultaneous-phase interferometer (995x1003 pixels). There are 700x700 pixels across the 180 mm pupil, so one pixel projects to 257 μm x 257 μm on the segment surface. Phase ambiguity occurs if there is $\geq 1\lambda$ OPD between adjacent pixels, i.e. $\geq \sim 316\text{nm}$ height variation over a lateral 257 μm on the surface. Theoretically, a pure tilt term of 221 μPV over the 180mm pupil would therefore correspond to the onset of phase-ambiguity. In practice, we find that the limit is approximately 140 fringes over the 180 mm diameter pupil, corresponding to a surface slope of approximately 246 μrad .

SURFACE TEXTURE AND MEASUREMENT

The ESO E-ELT specification requires a surface texture of <3 nm Ra (requirement) and <2 nm Ra (goal). Owing to the size and weight of the

segments, measurement using a standard bench top white light interferometer is impractical. On-machine techniques requiring contact with the polished surface, such as stylus and replication, are deemed too high risk and so avoided.

Texture measurement is carried out using the Surface Texture Analyser (STA1, or 4D NanoCam), provided by 4D Technology, Arizona. This device mounts directly into the tool spigot of the Zeeko IRP machine (FIGURE 1), thereby allowing the CNC system to position and align the device for measurement. The IRP machine range has been developed with small-footprint polishing processes in mind, such as millimeter-scale fluid jet polishing [5]. This positional accuracy allows the STA to be positioned at the same surface location following successive processing runs, to observe the effects of polishing on surface texture and sub-surface damage. In testing, it is observed that an on-machine positional repeatability of <9 μm can be achieved [6].



FIGURE 1. STA fitted to the IRP1600 tool-chuck, testing a full-size segment.

The use of CNC axes to position the measurement device also provides an opportunity to automate the testing process. Zeeko are completing development of a software application to allow the definition of a testing regime, in a similar manner to a polishing tool path (FIGURE 2). This measurement plan is then executed by the same application, which uses feedback from the interferometer automatically to align the device and command measurement. Following some initial set up tasks, user input is not required. Output data is

automatically saved and a report produced containing data statistics and an indication of measurement quality.

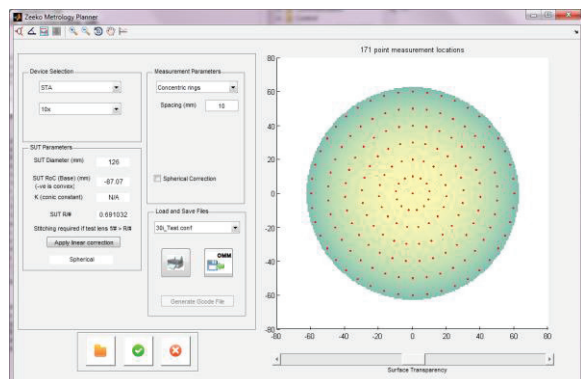


FIGURE 2. Example STA measurement plan created using Zeeko Metrology Control Suite.

FIGURE 3 shows an example measurement of the first segment completed by the consortium and certified by ESO. This measurement is typical of texture across the bulk of the surface at 0.93 nm Sa over a ~ 0.9 mm x 0.9mm area, well within the ESO specification. This measurement was obtained with a 10x objective, providing a pixel size of $\sim 0.7 \mu\text{m} \times 0.7 \mu\text{m}$, and has Zernike terms up to and including coma removed. Following smoothing, there is no evidence of pre-polish or sub-surface damage artifacts.

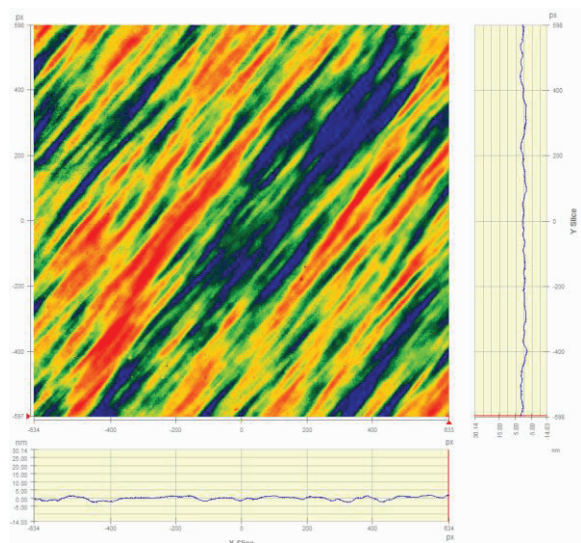


FIGURE 3. ESO E-ELT prototype segment texture measurement. $S_a = 1\text{nm}$ after removal of relevant form terms.

Such process automation will prove crucial in meeting production rates for the E-ELT.

TRACKING BASE-RADIUS

With monolithic primary mirrors, a small error in base-radius is accommodated by re-focusing the telescope. For a segmented primary this still applies – but only for the mirror as a whole – hence the ± 200 mm tolerance on base-radius. As mentioned above, the matching of segments is much more challenging, particularly over a production schedule that will span several years, and given thermal drifts in the 10m high Optical Test Tower. Removing the power term from interferometry data is not an option; the segments must be corrective-polished from *absolute* form-data, rather than data *relative* to the nearest-fit sphere.

The only tenable solution of which we are aware is a *transfer standard*. This must both be highly stable, and be smooth, though global form is less critical. We therefore polished an over-size $R=84$ m Zerodur Master Spherical Segment (MSS) to 18 nm RMS, with 4.2 nm RMS for spatial scales <100 mm RMS and 5.5 nm RMS for spatial scales <250 mm [9]. The blank is 200mm thick (c.f. 50 mm for segments), and so some 64 times stiffer than a segment. This allows mounting on a simple support system, further reducing uncertainties in form.

The MSS is deployed directly over the top of the segment, which is left undisturbed on the IRP1600 machine. Full-aperture segment interferometry is then bracketed with metrology of the MSS. The only other difference in the respective test configurations is the exchange of the segment-CGH for another designed for testing the sphere.

SEGMENT SUPPORT SYSTEM ISSUES

Support Requirements

Segment support systems are required to fulfil two purposes:-

- Under process forces, to maintain the geometric *position* of the segment in the machine CNC coordinate frame, and maintain *distortion* of the segment at a level that does not compromise the surface-figure for the process being used.
- For metrology, maintain distortion of the segment at a level that does not contribute

significant errors to the measurement of the surface-figure at the relevant process-step.

Grinding Support

For grinding the segment using the BoX machine, and subsequent measurement using a coordinate measuring machine (both at Canfield University) the segments were mounted on to a platen made of high grade aluminium, precision diamond-turned flat and parallel [2]. However, the rear surfaces of the segments are true and flat at the level of only 100-200 μ PV. Therefore, the support points/areas were ill-defined, leaving potential for i) unquantifiable gravity-induced distortions of the segment and ii) both rocking and distortion under grinding forces. Issue i) above would indicate a surface that is machined true to the design-form (and so verifiable by CMM data acquired with the segment on the same support platen), but the segment material not being in the stress-free state. Effects of ii) above should be visible in the CMM data.

The first prototype segment (Zerodur), ground to the off-axis asphere, was measured on its grinding support by CMM. The form was within XX μ PV, giving confidence in the grinding operation. The second segment (ULE) was also ground on the BoX, but CMM data was unavailable. Therefore, the part was given a rapid pre-polish on the Zeeko machine, just sufficient to glaze the surface for a preliminary interferometric measurement, and this revealed in excess of 40 μ PV form error. We suspect that the segment may have rocked in grinding, although it is possible that the Twyman effect contributed to this. Golini and Jacobs [7] have observed in regard to ULE that, “the grinding surface stresses, known as the Twyman effect, increased dramatically in the transition from brittle to ductile mode grinding”. In this context, it is notable that the BoX process exhibits aspects of ductile removal.

We have constructed a 1 m prototype hybrid support system, originally intended for polishing (FIGURE 4). This was a hydrostatic system using eighteen Bellofram rolling diaphragms, configured in three sets of six, each of the three sets actuated by a separate fluid-displacement actuator. Each Bellofram unit incorporated a bespoke ‘hydro-expandable’ locking mechanism, so that the part could first float, and then be locked in position. Hysteresis in the hydro-expandable locks proved excessive for

polishing, but this solution shows considerable promise for grinding, where the tolerances are in the micron rather than nm regime.



FIGURE 4. Original prototype hybrid lockable hydrostatic support system.

Polishing Support

Segments are polished on the Zeeko 1.6 m CNC polishing machine [8], mounted on a standardised support system, and measured in-situ by a variety of techniques:-

- Full-aperture nulling interferometry
- Sub-aperture stitching interferometry
- White-light texture interferometry
- Non-contact profilometry (autocollimator - pentaprism test)

The requirements on the support system for the metrology which closes the corrective process-loop, are a couple of orders more critical than for grinding. In particular, the support system is required to emulate that which will be used in the telescope, assuming the telescope zenith-pointing. In this configuration, corrective polishing will remove the signatures of gravity-deflections where ‘seeing’ will be best (minimum atmospheric air-path). At other zenith distances, there will be some degradation of performance.

Given this requirement, the segment is supported on a 27-point hydrostatic whiffle-tree (FIGURE 5). The support is based on Belloframs, configured in three sets of nine, each set with a separate fluid-displacement actuator. A metal diaphragm-flexure locates in a rear bore in the segment to provide lateral constraint. The precise locations of these support-interfaces on the rear of a segment, and the detailed interface designs, accord with the ESO specification [1]. Within the limits of residual hysteresis, this ensures that the

measured surface-form will truly reflect performance in the telescope, *providing* that additional constraints deployed to resist the lateral components of polishing forces are removed.

The hydrostatic whiffle tree is not suitable for polishing because it is “soft” and the segment can tip/tilt under vertical polishing forces as the hydrostatic fluid re-distributes. The effect of such motion is to disturb the Z-offset of the bonnet polishing process i.e. the bonnet compression against the surface, and so the polishing spot-size and volumetric removal rate. For this reason, the fluid-displacement actuators are used to lower the segment onto a separate support for polishing.

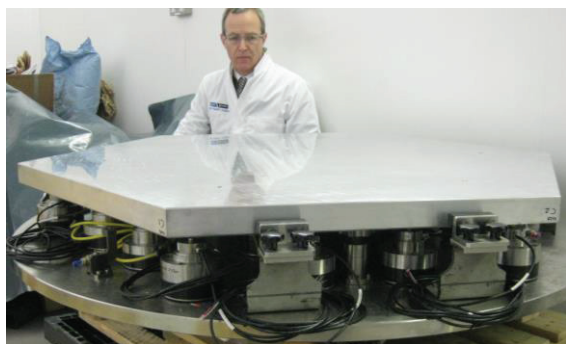


FIGURE 5. *Dummy full-size segment integrated with polishing/metrology support system.*

TIME FOR DATA-ACQUISITION AND COMPUTATION

The Optical Test Tower provides full-aperture null interferometry of the segment whilst in-situ on the Zeeko machine. A 4D Technologies Phase Cam 5030 interferometer provides simultaneous phase acquisition to freeze vibration. The 10m high air-column is randomised with fans. A typical measurement sequence, when near to final form, will involve acquiring multiple data-sets, each after exercising the hydrostatic support system to average residual hysteresis, and interleaved with data-sets acquired with the MSS inserted to provide re-calibration of the test-tower optics. This can amount to nearly 4,000 data-frames. The total time to acquire, store and process this data is ~3 days, whereas the total accumulated exposure is only a few seconds. The contributors to this overhead include:-

- CCD readout
- Frame-grabbing
- Disk storage

- Data-frame select/reject
- Data averaging of selected frames (tower thermal constants etc.)
- Calibration of the test-tower optical system from MSS data
- Data analysis to create final error map

In the context of segment serial production, it is clear that significant investment will be required in at least the following upgrades:-

- Higher-power laser for the simultaneous-phase interferometer
- Fast data acquisition hardware
- Improved IT infrastructure for data processing
- Additional software for pipe-lining and automating data acquisition and processing
- Improved thermal environment to reduce thermal ‘noise’ and so number of data frames required
- Automated MSS deployment

CONCLUSION

The prototype segment project has demonstrated effectively, for the first time anywhere, an end-to-end process chain that operates entirely on segments pre-cut to the final hexagonal shape, *whilst meeting the edge-quality specification.*

The first segment (designated SPN04) was the segment on which much of the process development and diagnostics was conducted. It received formal acceptance from ESO, after acceptance tests were witnessed by ESO at the National Facility site in North Wales.

The second segment SPN01 has been completed and is awaiting the ESO visit for acceptance tests. The final surface error was measured to be 25nm RMS, and 7.6nm RMS after removing the ESO low-order allowances.

These represent the first prototype E-ELT segments produced anywhere that have achieved surface residuals of this quality.

The third segment SPN03 has been ground to the aspheric form, and is being prepared for the polishing cycles on the IRP1600 machine.

The project has highlighted the complex interactions between manufacture and measurement in several notable aspects. These are accentuated by the need i) to assert

matching of segments as well as their overall form, and ii) by the base-radius of 84m which adds complexity to the optical test system, in order to avoid testing over long air-paths. The key issues are summarised below:-

- The interesting parallel between aspheric misfit in polishing and dynamic range in interferometry
- The critical effects (particularly approaching, the sub-10nm level) of residual support-system hysteresis on measurement.
- The benefits of conducting metrology entirely on the polishing machine, avoiding disturbance of the part, down-time, and potential accidental damage, when repeatedly removing and replacing the segment at each measurement/polish cycle
- The disadvantage of on-machine metrology, as metrology ties-up machine resource, when it could be polishing another segment.
- The need for a stable transfer standard to assert matching of base-radius of multiple segments over the several years duration of the manufacturing life-cycle for full-scale segment manufacture.
- The need to reduce the frequency of MSS calibrations by improving the thermal stability of the test environment
- The impact on total manufacturing time caused by data acquisition and processing overhead.

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A Study of Real-time Spindle Error Compensation in Single-Point Diamond Turning of Optical Micro-structured Patterns on Precision Rollers

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INTRODUCTION

Micro-structured patterns are widely used in optics since the optical performance can be significantly improved in many applications [1]. One of the most common methods to fabricate the micro-structure is using Single-Point Diamond Turning on Precision Rollers [2]. The accuracy requirement of the Precision Rollers is stringent because the dimension of the micro-structure is very small (pitch lengths and depths 10-100 μ m) and surface finishing is ultra-smooth ($R_a < 3$ nm) [2]. In order to achieve this level of accuracy, the manufacturing errors of the machine tool are required to be reduced while error compensation methods are needed to be developed.

Spindle errors can be classified as synchronous error and asynchronous error [3]. Synchronous error occurs at integer times of spindle rotation frequency and can be represented as repeatable error while asynchronous error occurs at non-integer times of spindle rotation frequency and can be represented as non-repeatable error. Most of the existing error compensation techniques are based on offline error compensation methods (OECM) which can only compensate the synchronous error [4,5]. The asynchronous error is fluctuating without a predictable value and it is different from the synchronous error so it cannot be eliminated using OECM. One of the most promising methods to compensate the asynchronous error is real-time error compensation method (RECM). Some researchers have studied the RECM and their results showed that it was effective to enhance the machine accuracy [6,7].

However, most of the previous research work is focused on the machine tools with a relatively low accuracy and there is relatively few studies focused on the Single-Point Diamond Turning. Kim and Kim developed a feed-forward control of fast tool servo system for real-time correction of spindle error for diamond turning of flat surfaces [8]. A capacitive displacement sensor was used to measurement the spindle axial error motion and the motion error was compensated using a fast tool servo. A flatness of 0.1 μ m was achieved with a 100mm diameter aluminum specimen. However, the study only considered the axial error, when it is diamond turned on precision rollers, both the radial error and axial error have to be compensated.

This paper attempts to investigate the RECM in Single-Point Diamond Turning of Optical Micro-structured Patterns on Precision Rollers. The radial error and axial error were simulated and the compensated results of OECM and RECM were presented considering both synchronous errors and asynchronous errors in radial and axial directions. The results of OECM and RECM were also compared and discussed. Furthermore, the effects of time delay in RECM were studied. An adaptive time-series modeling method was also proposed to predict the real-time error to reduce the time delay effect of RECM. The results show that the RECM is effective and promising to further improve the accuracy of the Single-Point Diamond Turning Precision Rollers.

ERROR MEASUREMENT AND COMPENSATION METHODS FOR PRECISION ROLLERS

Fig. 1 shows the diagram of the RECM system for the three-axis (X, Z and C) for Single-Point Diamond Turning Precision Roller. One end of the workpiece is mounted on the air spindle and the other end is mounted on the air bearing. There are two capacitive sensors, capacitance sensor A (CSA) and capacitance sensor R (CSR). CSA are mounted perpendicular to the end surface of the workpiece so as to measure the axial error. CSR is mounted on the tool holder and parallel to the cutting tool which is perpendicular to the machining surface to measure the radial error. The distance from the cutting tool to the CSR is as short as possible to ensure the difference of radial errors between the cutting tool and the CSR can be neglected.

In this implementation, the CSA and CSR measurement directly measure the relative movements between workpiece and cutting tool axially and radially, respectively. Accordingly, the diamond cutting tool is driven by a two degree of freedom fast tool servo (2-DOF FTS) in order to compensate the radial and axial errors. The FTS is mounted on the X-Z slides to carry out movement at the full X-Z range.

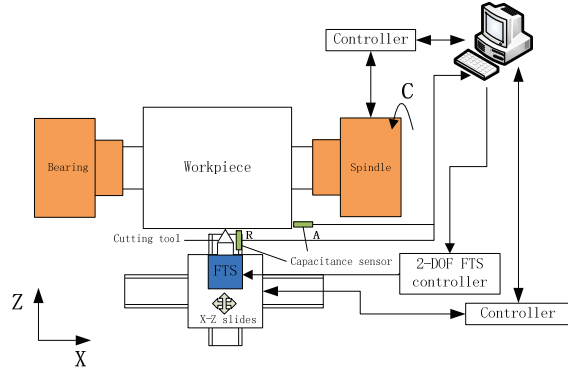


Fig. 1. Diagram of the online real-time error compensation system

The time delay from the moment when the workpiece is being machined to the moment when the error is measured and compensated is critical and it should be short enough to achieve a better compensation result. In order to fulfil this requirement, the chain of the system needs to be short. Fig. 2 shows the flowchart of the online real-time error compensation system. There is a tight loop to detect the CSA and CSR signals, calculate the radial/axial errors and send compensation commands to the 2-DOF FTS, which results in a better real time

performance. With some commercial capacitive sensor products, the bandwidth can be as high as 50 kHz [8], which is very promising for the real-time measurement. For the 2-DOF FTS, Zhu and Zhou [10] developed a mechanism with decoupled motions which bandwidth is up to 200 Hz. Polit and Dong [11] obtained a bandwidth of 2 kHz with their design of a piezo-driven nanopositioning stage, which is also suitable for the real-time compensation.

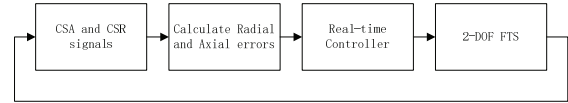


Fig. 2. Flowchart of the online real-time error compensation system

SIMULATIONS FOR OEMC AND RECM

The total machine tool error consists of synchronous error (E_s) and asynchronous error (E_r) and it can be expressed in Eq. (1). E_s is repeatable, which was simulated using sine signals (f_1 at frequency) with different amplitudes. While the E_r is non-repeatable, which was expressed as random offsets plus lower frequency sine fluctuating signals (at frequency f_2 , as shown in Eq. (2)). The average amplitude E_s of and random offset were given as 200 nm. Fig. 3 shows the simulated radial error and the axial error. The center lines with plus sign marker (+) denote the E_s while the solid lines with a broader range denote the total errors. The simulated results can match the real error properly as comparing with the signal provided by Aerotech air spindle ABS2000 [12].

$$E_t = E_s + E_r \quad (1)$$

where E_t is the total error, E_s is the synchronous error and E_r is the asynchronous error.

$$E_t = E_s + E_r = E_{f1} + Offset + E_{f2} \quad (2)$$

where E_{f1} is the synchronous error with frequency f_1 , $Offset$ is the random offsets for each cycle or revolution and E_{f2} is another part of the asynchronous error represented by a sine signal with frequency f_2 .

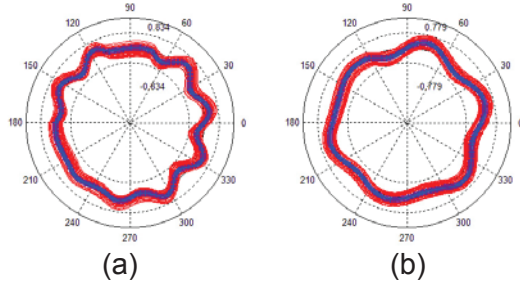


Fig. 3. Simulated errors. (a) Radial error and (b) axial error. (Radius unit: μm)

SIMULATION RESULTS FOR OEMC

For the OEMC, E_s can be measured using various kinds of measurement methods [13, 14]. However, E_r cannot be measured using offline method so the OEMC only compensate E_s and there will be remaining error (E_{re}) which cannot be compensated. The E_{re} can be expressed in Eq. (3) and it equals to E_r . Fig. 4 shows the E_{re} after OEMC for radial direction and axial direction, respectively. Since E_s were compensated, the main deviations contributed from E_s were eliminated and the error amplitudes were reduced (radial: from 765nm to 570nm; axial: from 726nm to 432nm). However, E_{re} contributed from the asynchronous error still has significant values that affect the machined surface significantly.

$$E_{re} = E_t - E_s = E_r \quad (3)$$

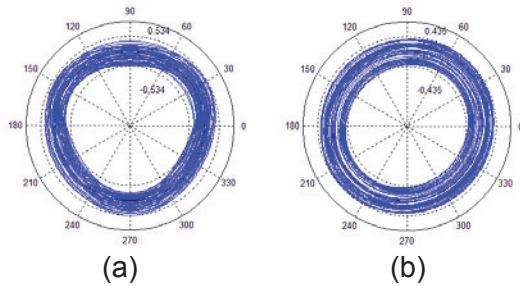


Fig. 4. Remaining errors after offline compensation. (a) Radial error and (b) axial error. (Radius unit: μm)

SIMULATION RESULTS FOR RECM

With the RECM, both E_s and E_r can be measured and compensated. Ideally, the compensation result has E_{re} which is equal to zero. However, the implementation of error measurement and error compensation (both hardware and software systems) require a certain amount of time, which causes the cutting tool to

compensate the error only after the spindle has rotated a certain degree (RD) of angular rotation. Hence, the E_{re} is influenced by this effect and can be expressed in Eq. (4).

$$E_{re} = E_t - E_{shift} \quad (4)$$

where E_{shift} is the error value shift from E_t .

Fig. 5 shows the E_{re} after RECM at the condition $\text{RD}=1^\circ$. The results show that the E_{re} have been significantly reduced as comparing with the OEMC results as shown in Fig. 4. The amplitude of radial error was reduced from 570nm to about 40nm and the amplitude of axial error was reduced from 432nm to about 60nm. The wide band amplitudes of error fluctuating was narrow down by the RECM. The reduced E_{re} comes from the effect of RD and it is relatively small as compared to E_r . The results demonstrated that the RECM is effective and it can improve the accuracy of the machining process.

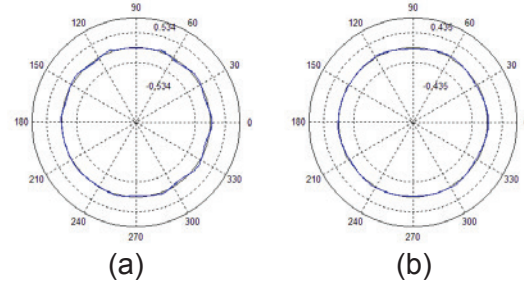


Fig. 5. Remaining errors after online real-time compensation, $\text{RD}=1^\circ$. (a) Radial error and (b) axial error. (Radius unit: μm)

Fig. 6 compares the E_{re} after RECM at conditions $\text{RD}=1^\circ$ and $\text{RD}=4^\circ$. The solid lines and thicker lines with plus marker (+) denote the compensated results when $\text{RD}=4^\circ$ and $\text{RD}=1^\circ$, respectively. The E_{re} denoted by solid lines are fluctuating at much wider range of amplitudes than that denoted by the thicker lines with plus marker (+). The results show that the compensation performance is greatly reduced as RD increases. Comparing with the offline compensation results as shown in Fig. 4, although the online real-time error compensation has the ability to reduce the total error, it has a drawback that the results contain a high frequency noise which may affect the machined surface.

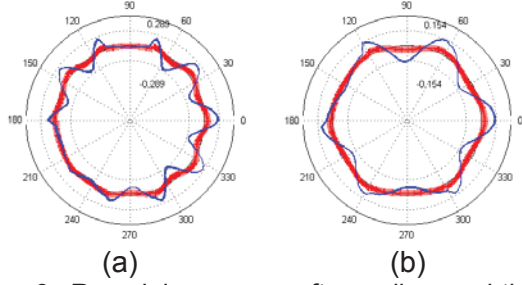


Fig. 6. Remaining errors after online real-time compensation, solid line: RD=4° and thicker line with plus marker: RD=1°. (a) Radial error and (b) axial error. (Radius unit: μm)

The relationship between the angle shift (A_s) and the time delay (T_d) can be determined by Eq. (5). In an experiment setup with machined workpiece radius = 50mm, spindle speed = 1000 rev/min, the time for 1° shift is 9.5ms which is a reasonable time for most real-time system with the capacitance sensor (50 kHz bandwidth) and FTS (2 kHz bandwidth). As r increases, T_d decreases and it requires a faster real-time system with better processor, sensor and FTS which may significantly increase the cost. For this reason, it is recommended to limit the spindle speed to less than 2000 rev/min with $T_d = 4.8\text{ms}$.

$$T_d = 30A_s / (\pi \cdot r) \quad (5)$$

where r denotes the spindle speed (rev/min).

ERROR PREDICTION USING ADAPTIVE TIME-SERIES MODELING METHOD

In order to reduce the time delay effect of RECM, an adaptive time-series modeling method is proposed to predict the real-time error. In the adaptive time-series modeling method, an n -th order autoregressive model is used to determine the current error value which is a linear combination of the n previous error values and it can be expressed as Eq. (6).

$$\hat{y}(k) = A(k)Y(k-1) \quad (6)$$

where $A(k) = [a_1(k) \ a_2(k) \ \dots \ a_n(k)]$ is the coefficient vector,

$Y(k-1) = [y(k-1) \ y(k-2) \ \dots \ y(k-n)]^T$ is the n past signal vector and $y(k)$ is the sample of the real-time error.

The difference of the predicted error value and the true error value is determined by Eq. (7) and the update process of the coefficient vector is determined by Eq. (8).

$$e(k) = y(k) - \hat{y}(k) \quad (7)$$

$$A(k+1) = A(k) + \beta e(k)Y(k) \quad (8)$$

where β is the constant adaptation gain to determine the adjustment step size of A .

The selection of β is important since it affects the prediction accuracy and stability of the model. Fig. 7 shows different $e(k)$ values when β equals to 5×10^4 , 5×10^5 and 5×10^6 respectively. $e(k)$ has the smallest value when β equals to 5×10^5 , while $e(k)$ becomes extremely large (over 10^{300}) when β equals to 5×10^6 which indicates the model enters an unstable state. The periodical peak values in the graphs may come from the transaction of cycles of the simulated errors. The result shows that a well-tuned adaptive time-series modeling method has a prediction error less than 150nm and which is comparable to RECM with RD=4°. Future work will be conducted to further fine tune the adaptation gain.

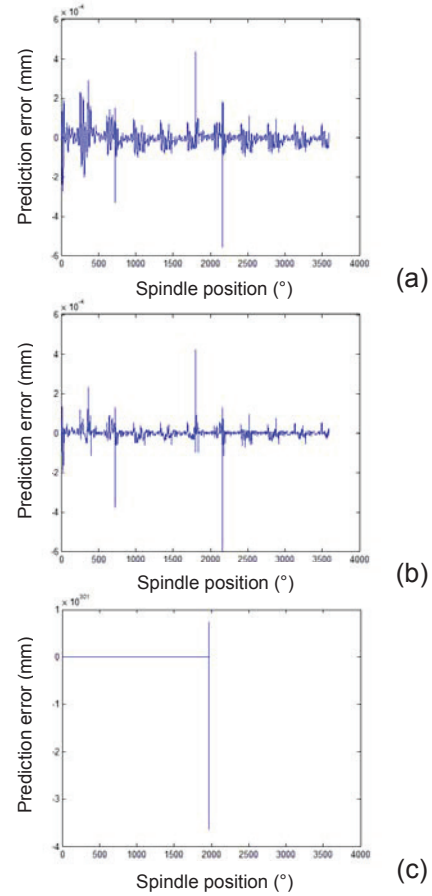


Fig. 7. Different $e(k)$ values at (a) $\beta = 5 \times 10^4$, (b) $\beta = 5 \times 10^5$ and (c) $\beta = 5 \times 10^6$

CONCLUSIONS

In this paper, the spindle errors of a Single-Point Diamond Turning machine were simulated with the synchronous and asynchronous errors for both radial and axial directions. The offline error compensation method (OECM) and the online real-time error compensation method (RECM) were compared and different results were obtained. The effect of time delay on the online real-time error compensation was also studied. Furthermore, an adaptive time-series modeling method is proposed to predict the real-time error. The main conclusions can be summarized as follows.

1. Machine tool errors consist of synchronous error and asynchronous error. OECM can only compensate synchronous error while RECM is able to compensate both synchronous error and asynchronous error. The latter one is more effective and promising.
2. Time delay for RECM is one of the most important factors affecting the compensation result. A fast real-time system with high performance processor, sensor and FTS is most needed and a relatively low spindle speed is recommended.
3. The adaptive time-series modeling method is effective to predict the real-time error so as to reduce the effect of time delay. A well-tuned adaption gain is much needed which will be further studied

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A NANOPROFILER USING THE NORMAL VECTOR TRACING METHOD FOR HIGH ACCURACY ASPHERIC MIRRORS

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INTRODUCTION

For the lithography, using extreme ultraviolet light with a wavelength of 13.5nm (Extreme Ultraviolet Lithography; EUVL) and X-ray free electron laser (XFEL) and third-generation synchrotron radiation facility, in a lot of digital video equipment such as medical equipment and spaceborne imager, high-quality camera, and a projector, the optical elements of the lens and high-precision mirror having a shape of a variety are requested. Aspherical amount of 15 μ m or more, the accuracy of shape error 1 nm PV is required for the next-generation high-precision mirror used hard X-ray of the XFEL and third-generation synchrotron radiation facility, the soft X-ray region of EUVL. Further, the aspherical mirror is required for the imaging optical element of the artificial satellite, in order to improve optical characteristics (bright, high resolution) and reduce the weight and size of the optical system, in comparison with the spherical mirror. In this case, the aspherical amount is greater than 100 μ m, 10 nm PV or less is required to figure errors.

For the next generation of ultra-high-precision mirror lens production as described above, significant progress of ultra-precision measurement technology and ultra-precision machining technology is essential. Accuracy an order of magnitude than the processing technology is necessary to measurement technology. As the measurement technology of undulation and surface roughness of less than 1 mm space wavelength, there is an atomic force microscope and scanning tunneling microscopy, and scanning white light interference microscope, etc., required accuracy is satisfied at present. On the other hand, as the shape measurement technique at least 1 mm spatial wavelength, there is stitching interferometry or phase-shifting Fizeau interferometer, and a coordinate measuring machine etc., but the linear motion mechanism as a reference and the reference plane is always required, and the required shape measurement accuracy of 1 nm

PV is not filled with free-form surfaces [1–2]. Thus, measurement method capable of measuring the shape accuracy of 1 nm PV free-form surface essential for next-generation high-precision mirror fabrication is none at this time.

The aim is to meet the above requirements, we have developed a nanoprofiler. Error reduction of nanoprofiler is carried out by the encoder calibration [3] and the optical system design [4] etc., high-precision measurement of spherical and plane surface have been performed [5–6]. Examples of the measurement of the aspheric shape is shown in this paper

NANOPROFILER

The aimed measurement object for nanoprofiler is aspherical (free-form) shape that have diameter ϕ 300 mm maximum, amount of asphericity from 10 μ m to 1 mm. The target specification of nanoprofiler is as follows.

1. The reproducibility of 1 nm (standard deviation) or less.
2. 10 nm or less uncertainty.
3. Slope error 0.1 μ rad below.
4. Measurement time of 5 min / sample.

Figure 1, the principle of the fast nanoprecision shape measuring method according to the normal vector tracing for measuring the free-form surface configurations that independently proposed is shown by utilizing the straightness of the laser, using a rotational motion with high accuracy compared with the translational motion, the shape measuring method determines the shape from measuring the measurement point coordinate and the normal vector of the mirror [7–8]. By adjusting the 2 sets of 2 axes of goniometer, the laser light emitted from the light source is reflected by the mirror surface, reflected light back to the center of the quadrant photodiode detector (QPD) located at the equivalent optically to the light source, then normal vector at that point is obtained. Further, in the translation stage in the y-direction, by

adjusting the optical path length L is constant at the same time, measuring point coordinate (X_p , Z_p) is obtained as a result. Stage consisting of single-axis translation and two-axis goniometer and an optical head made up of a light source and QPD is referred to as the optical head motion unit. On the other hand, two-axis goniometer the sample is held is referred to as the sample motion unit. By the numerical control of two axes goniometer of sample motion unit, measuring point coordinate is changed. Normal vector is tracked using a biaxial goniometer of the optical head motion unit at the same time. Further, so as to maintain a constant optical path length L , y axis is numerically controlled. It is expected that by 5-axis simultaneous control, 5 min / sample measurement time is achieved. In this measurement method, without using a reference surface, there is no limit on the measurement geometry in principle. Shape measurement of the free-form surface is possible [9].

Schematic view of nanoprofiler is shown in figure 2. In the optical head motion unit, there are a goniometer two axes rotating in a direction that is independent (θ, ϕ), the translation stage in one axis (y). An optical head mounted on the optical head motion unit comprises a laser light source, the detector QPD located at the optically equivalent to the light source. Then, QPD and laser are placed in a rotation center of the two axes. In the sample motion unit, there are goniometers of two axes rotating in a direction independent (α, β). The rotary encoder is 1.5 nrad resolution, the positioning accuracy of the translation stage is 1 nm. The normal vector is calculated using the output value of the QPD and the rotation angle of the goniometer ($\theta, \phi, \alpha, \beta$) in the measurement process. The measurement coordinates are determined by reading the displacement of the translation stage and the rotational angle of the goniometer.

It is necessary to obtain the shape from the normal vector and coordinate measuring point. Since the slope of the shape itself normal vector, shape can be obtained if the integral slope. However, in the angle of inclination integration method, error accumulates in the shape when number of measurement points is increased. Therefore, an algorithm for fitting the derivative of the shape model function by Fourier series to measured normal vector are used.

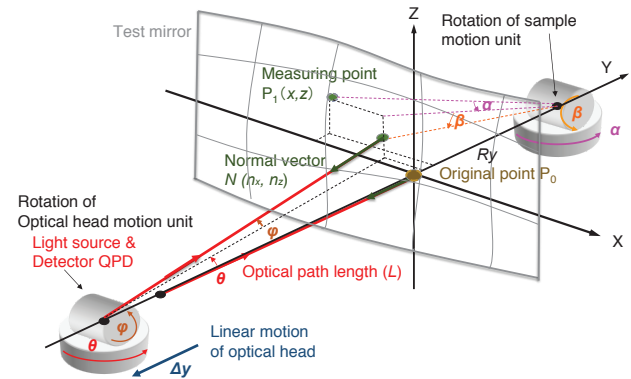


FIGURE 1. Principle behind our system for profile measurement in two dimensions. [6]

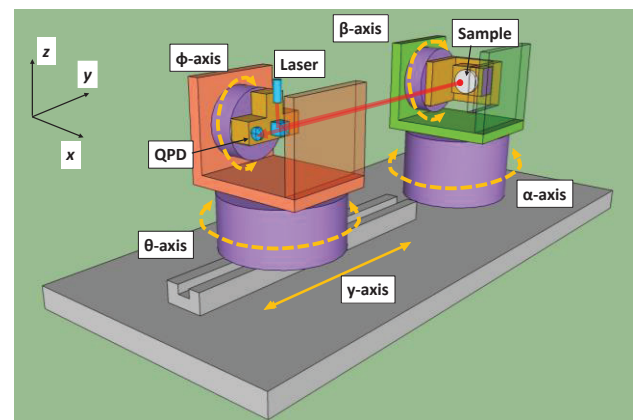


FIGURE 2. Schematic view of nanoprofiler.

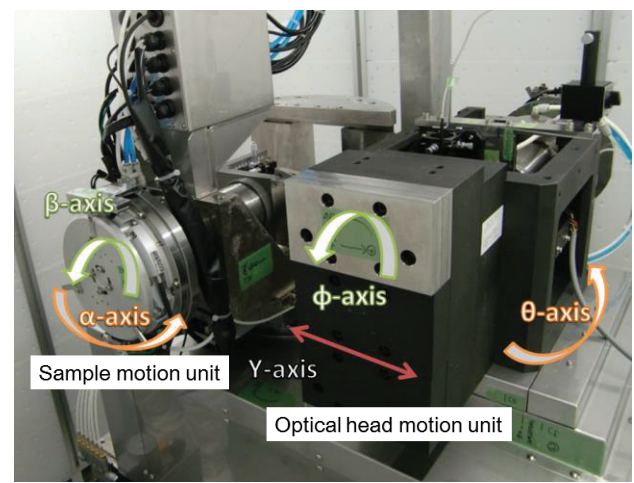


FIGURE 3. Photograph of nanoprofiler. [6]

MEASUREMENT OF ASPHERIC MIRROR

Aspheric concave spherical metal mirror was processed by the super-precision cutting was measured. Aspheric shape is shown as follows.

$$X = \frac{Y^2/R}{1 + \sqrt{1 - (K+1)Y^2/R^2}} + \sum_{n=0}^m C_n Y^n$$

$R = 500.000$ $C_2 = 0.00000E-00$
 $K = 0.0000$ $C_4 = 588234134322E-09$
 $C_6 = -6.02541612923E-15$
 $C_8 = -2.71914997789E-21$

Measurement range $20 \times 20 \text{ mm}^2$ is measured in a raster scan system. The difference between the ideal shape and the obtained measurement result is a figure error. The figure 4 shows the result of averaging the figure errors. It is 59.2 nm PV calculating the PV value of the surface of the aspherical mirror of the difference between the maximum and minimum values of the figure error. Take the standard deviation at each measurement point, to assess reproducibility as the average value of the standard deviation. 0.371 nm reproducibility (standard deviation) is obtained, the target 1 nm is achieved.

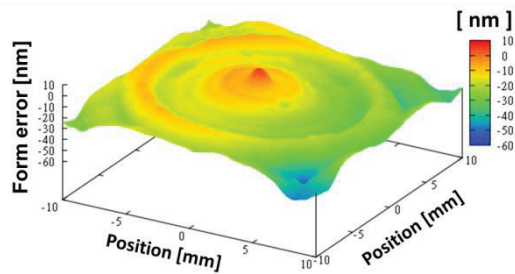


FIGURE 4. Measurement of the figure error of the aspherical mirror

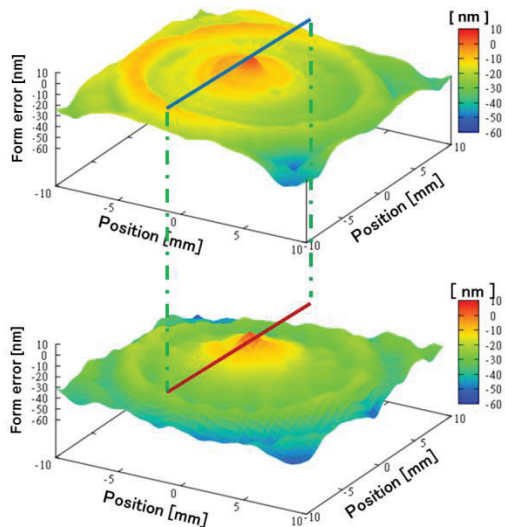


FIGURE 5. Comparison of the shape measurements of aspherical mirror due to the phase shift Fizeau interferometer (bottom) and nanoprofiler (top)

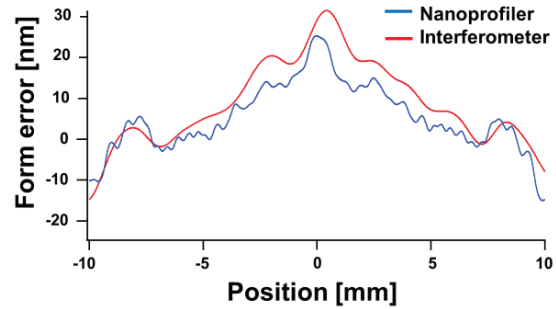


FIGURE 6. Comparison of the cross-section curve measurements of the center line of the aspheric mirror by the phase shift Fizeau interferometer and nanoprofiler

To evaluate the shape measurement result of the aspherical mirror by nanoprofiler, is compared with the shape measurement result obtained by phase shifting Fizeau interferometer using a null lens. Comparison with the phase shift Fizeau interferometer and nanoprofiler is shown in figure 5. Figure error of nanoprofiler is 59.2 nm PV, figure error in the phase shift Fizeau interferometer is 61.4 nm PV. Furthermore, comparison of the profile curve measurement results shown in the line of figure 5 is shown in figure 6. It can be concluded, as shown in figure 6, the difference in the shape measurement result of the two are consistent within the range of about 10 nm, and is consistent with systematic errors within the measuring device, respectively.

Thus, the reproducibility (standard deviation) of the evaluation value of the influence of random errors, nanoprofiler is achieved the target 1 nm or more Effect of systematic error is estimated to be comparable to the phase shifting Fizeau interferometer.

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LITHOGRAPHY OF COMPUTER-GENERATED HOLOGRAMS FOR FREE-FORM AND ASPHERIC SURFACE METROLOGY.

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INTRODUCTION

The use of aspheric and other complex surfaces has the ability to both simplify design and improve performance in optical systems ranging from consumer electronics to high-energy x-ray research. However, wide adoption of complex surfaces is still limited by high costs largely driven by the difficulty of measuring form errors on surfaces that are not flat or spherical. Interferometric measurement is a fast and well-understood technique for the measurement of aspheric optics provided that an accurate reference wavefront can be generated. Computer-generated holograms (CGH's) provide one means of generating nearly arbitrary optical wavefronts. Unfortunately, patterning of high-quality and affordable CGH elements is difficult using common lithographic methods.

A CGH is a diffractive-optical element, composed of quasi-periodic lines that diffract an incident beam so as to produce a desired wavefront. Commercially-available CGH's are generally produced by lithography tools developed for the semiconductor industry. Although that industry requires lithography capable of deep-sub-100 nm linewidths at high throughput, the industry does not require total freedom from distortion or long-range spatial-phase coherence in its patterning. Moreover, the substrate types accommodated by semiconductor-industry tools are limited to <1 mm-thick silicon wafers and <6 mm-thick glass plates for photomasks.

LumArray, Inc is developing a new maskless photolithography technology, zone-plate array lithography (ZPAL), which has the ability to address the shortcomings of other lithographic techniques for applications such as CGH [1-4]. The ZP-150 zone-plate array lithography tool, shown in Figure 1, has the potential to provide much higher quality CGH's and other diffractive optics by virtue of a system design that deliberately addresses error sources that degrade pattern placement in other maskless lithography tools.

ZONE-PLATE ARRAY LITHOGRAPHY (ZPAL)

In ZPAL, a large array of diffractive-optical lenses, typically Fresnel zone-plates, is used to focus a set of parallel input beams into focal spots on a scanning substrate. Using a spatial-light modulator, each beam to a specific zone plate is individually modulated on, off, or to an intermediate a gray-level. An optical system maps each pixel of the modulator onto a lens in the zone-plate array. By precisely coordinating the scanning of the substrate with the modulated light, complex patterns of arbitrary geometry can be created. This is illustrated schematically in Figure 2. Conceptually, this is similar to an ink-jet printer where a large array holes each dispense a small, controllable spot of ink as the head is scanned.



FIGURE 1. LumArray's ZP150 lithography system.

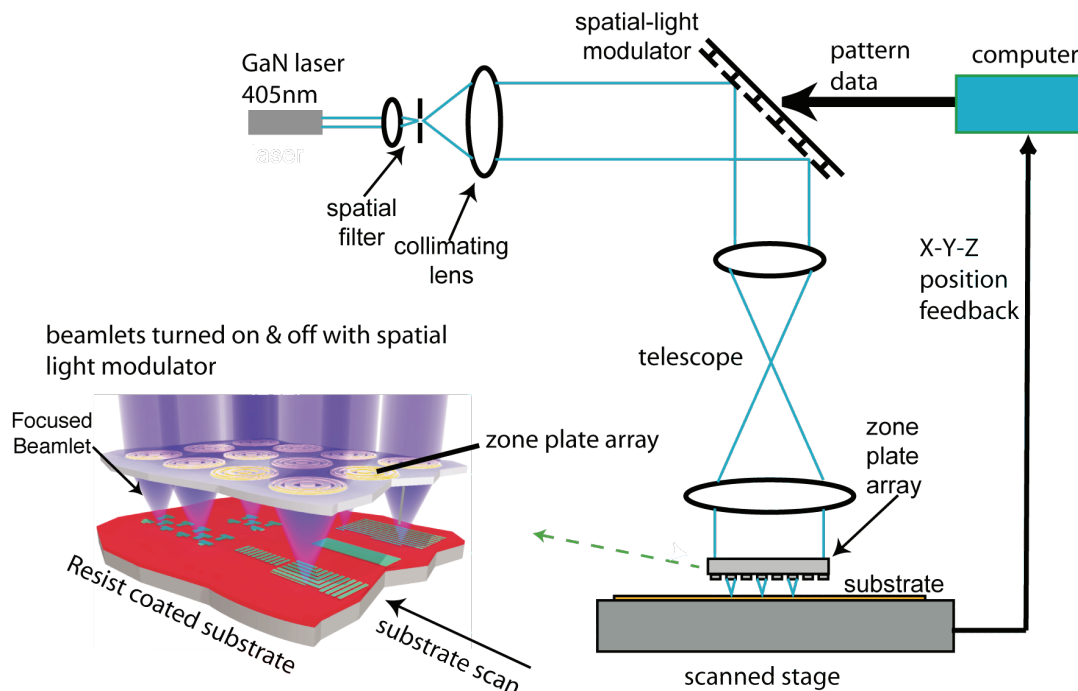


FIGURE 2. A CW laser illuminates a spatial-light modulator (SLM), and is directed to an array of zone-plates that focus light to diffraction-limited spots. Each pixel of the SLM addresses one zone plate. Pixel modulation is coordinated with the scanning stage to create patterns of arbitrary geometry in a dot-matrix fashion.

The ZP-150 system use a GaN laser at $\lambda = 405$ nm and the Grating Light Valve (GLV) modulator from Silicon Light Machines, Inc. The light output for all 1088 pixels can be independently adjusted from zero to full intensity through 256 gray levels at a frame rate of up to 290 kHz. In the ZP-150, arrays of up to 1000 zone-plates are used with numerical aperture (NA) of either 0.6 or 0.85. Scanning is performed with a custom-designed 2-axis air-bearing carriage riding on a lapped granite base with a position resolution of 1.22 nm. The system is enclosed in a mini-environment providing ISO Class 5 particulate control and temperature stability to 0.1°C. A summary of specifications is given in TABLE 1.

Illuminated on-axis, zone-plates are aberration-free producing diffraction-limited focal spots even at high-NA. Unlike refractive microlenses,

large arrays of high-NA zone plates (up to ~ 0.95) can be made with great uniformity.

<i>Exposure Wavelength</i>	405 nm
<i># parallel beams</i>	up to 1000
<i>Min. linewidth</i>	250nm @ 0.85 NA 350nm @ 0.60 NA
<i>Writing Speed</i>	Up to 1.5 mm ² /sec
<i>Grayscale</i>	256 levels
<i>Pattern Area</i>	150mm x 150mm
<i>Substrate Thickness</i>	Up to 35mm

TABLE 1. Summary of ZP-150 specifications.

As with any optical lithography, the minimum linewidth W of ZPAL is limited by the NA, the wavelength λ , and a proportionality constant k_1

$$W = \frac{k_1 \lambda}{NA}, \quad (\text{Equation 1})$$

In comparison to conventional optical-lithography at the same wavelength and NA, in ZPAL the patterning is done by the incoherent addition of “spots” rather than by partially coherent image projection, allowing precise control of the deposited dose at every location in the pattern. This precise dose control enables smaller features to be printed at a given

exposure wavelength than is commonly found in other optical lithography. Values of k_1 below 0.3, and feature sizes less than 200nm have been demonstrated using ZPAL at $\lambda=405\text{nm}$ [5].

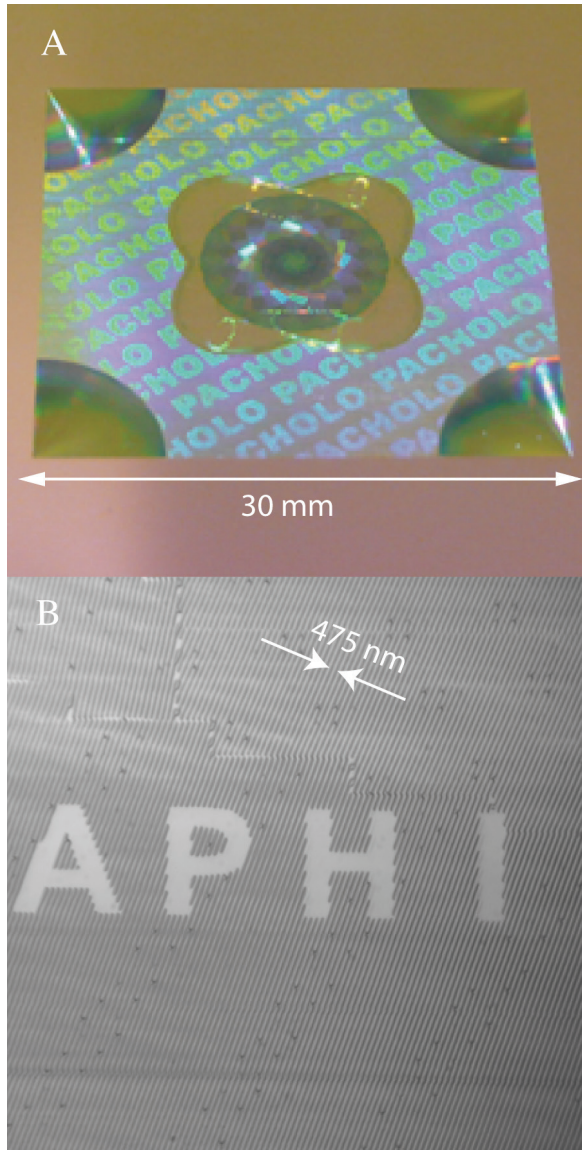


Figure 3. A) Example of a 30mm x 30mm CGH pattern in used for anti-counterfeiting labels or similar commercial applications. B) Magnified view of CGH shows the large-scale patterns seen in A are comprised of sections of diffracting lines with different pitch and orientation, with lines as small as 475nm.

APPLICATION TO CGH PATTERNING

The ZP-150 offers a unique combination of capabilities ideally suited for addressing applications, such as computer-generated holograms, that require low-volume or one-off patterns over large areas with wavelength-scale features. When patterning a CGH for metrological applications, the most relevant metric is wavefront accuracy in the diffracted beam. The ZP-150 has been designed from its conception to meet this need.

High Speed at High Resolution

Writing speed from the massively parallel lens array is up to $\sim 1.5\text{mm}^2/\text{sec}$ even at high resolution. Unlike electron beam lithography or other laser writers, patterning speed is independent of feature size and density. Exposure times for areas up to 150mm x 150mm are measured in hours rather than days even at maximum resolution.

In the ZP-150, the relatively short exposure times combined with a novel design of the metrology frame, make the accuracy of the ZP-150 far more robust to environmental changes than other pattern generators or ruling engines.

Patterning on thick, optically flat substrates

In CGH applications, wavefront flatness is limited by both lithographic accuracy and by poor flatness of insufficiently thick substrates. Photomasks are generally no thicker than 6.35mm, which makes it challenging to achieve flatness better than 500nm [5]. Traditionally, this has been difficult to overcome due to thickness limitations on the substrates used in the maskwriters used to pattern a CGH.

One technique used to address this problem is to map the flatness or total transmitted wavefront error of a substrate first, and then design a correction factor into the CGH pattern. LumArray's ZP-150 tool enables a better solution: pattern the CGH on thicker, flatter substrates. The ZP-150 is capable of patterning on true optical flats with flatness of $\lambda/20$ or better. The ZP-150 can accommodate substrates at least 35 mm-thick, even thicker with custom fixturing.

Precision Metrology for Low Wavefront Error

The most important position information for accurate patterning in ZPAL systems is the position of the substrate relative to the zone-plate array. In a traditional design, a laser interferometer will only measure stage position relative to the machine frame, leaving the optics

(or electron gun) to “float”. Measuring the position of these elements with an additional set of interferometer axes is possible, but adds substantial complexity and cost and is prone to error. Moreover, reliance on the machine frame as a reference makes a system susceptible to vibration, thermal expansion, and air-path issues such as humidity and turbulence.

In the ZP-150 we have eliminated many of these issues with a novel metrology-frame design that directly measures stage position relative to the zone-plate array, maximizing stability and repeatability while minimizing the impact of environmental changes. The metrology frame consists of a static upper plate and a lower plate mounted on the scanning stage. A two-dimensional encoder grid and the zone-plate array are rigidly mounted together in the upper plate. In a matching configuration on the stage, the encoder read-head is mounted on the lower plate with the substrate chuck.

Thus, the measurement directly tracks the motion of the wafer with respect to the zone-plate array. Any relevant motions of the machine structure due to thermal expansion, vibration, or other deformations are measured along with the stage scan and automatically corrected by the motion controller. The use of a 2D encoder grid instead of stacked single axis encoders eliminates the possibility of axis misalignment or hidden cross-axis parasitic errors. Additionally, the grid-reading mechanism is achromatic and therefore insensitive to environmental changes of the laser wavelength that can plague interferometers.

Because the optical column is fixed and *only* the stage position is scanned, this metrology design effectively allows closed-loop positioning of the focal spots on the wafer. Relative positions of the many parallel beams to each other are fixed by the zone-plate array and can be accurately calibrated. In comparison, in scanning beam systems, either laser or electron beam, the true beam position on the substrate cannot be tracked and must be inferred from the state of the scanning mechanism. In the ZP150, pattern data and exposure timing are controlled directly by stage position. This prevents the pattern data from coming out of synchrony with the stage, automatically correcting for random position errors and motion jitter.

The primary goal of this metrology configuration is to produce a positioning system with nm-level precision; absolute accuracy of the components is not required. It is unnecessary to eliminate error entirely, only to ensure errors that do exist are systematic. Repeatable errors that are difficult or impossible to fix directly can be calibrated and corrected via software compensation. Distortion in the encoder grid is one example of such systematic errors.

Measurements of placement error in linear diffraction gratings over 9 months have shown a repeatability of the error map in the ZP-150 of <30 nm. Similarly, patterns written including a correction for the measured error have shown placement accuracy of <30 nm. We expect that as our measurement protocol improves, the repeatability of the measured error will improve. Through improvement in the calibration process, we believe that pattern placement in a calibrated ZP-150 tool can be less than 10nm over the entire pattern area of 150mm x 150mm. This corresponds to wavefront error of $<\lambda/100$ from a CGH with features as small as 1 micrometer.

In addition to the ability to produce more accurate CGH devices, the combination of high accuracy with small feature sizes can expand the range of applications to which CGH-based interferometry can be applied. To preserve accuracy in the diffracted wavefront, most CGH devices are made with minimum linewidths on the order of 10 microns. Achieving both high resolution and low placement error, CGH devices with larger diffraction angles can be made using the ZP-150. This allows for test surfaces with greater aspheric departure or slope to be measured without the need for additional optics. Moreover, the separation of spurious orders diffracted by the CGH can be improved at higher diffraction angles.

VARIABLE LINE-SPACE DIFFRACTION GRATINGS: A BASIC CGH

The simplest CGH devices are diffraction gratings. If the line spacing in the grating can be made variable, then the deviation from a linear phase progression in the grating can be used to compensate for aberration in devices such as spectrometers and monochromators. In some cases, the ability to include aberration correction in the grating can eliminate the need for tight-tolerance aspheric elements elsewhere in a system.

In collaboration with the Lawrence Berkeley National Laboratory and funded through the Department of Energy, LumArray is currently evaluating the suitability of the ZP-150 tools for fabrication of variable line-space gratings to be used in high-energy x-ray and free-electron laser experiments at US national laboratories. Similar applications exist in satellite optics for astronomy research. General requirements for such diffraction gratings are similar to other high-precision CGH applications – micron or sub-micron features covering large area on thick, optically flat substrates with extremely high line position accuracy. An example of a 600 line/mm test grating on a 5mm-thick silicon substrate is shown in Figure 4.

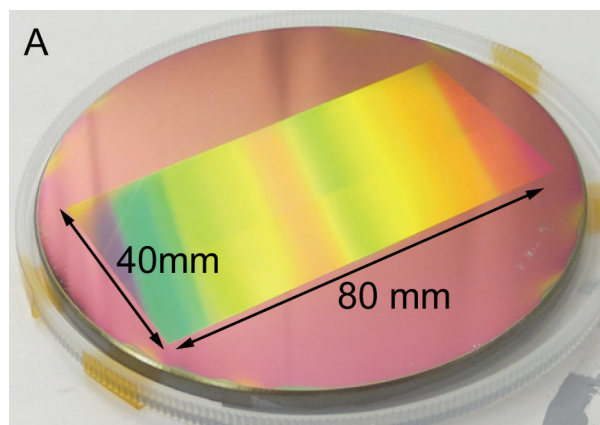


Figure 4. 600 line/mm diffraction grating covering 40mm x 80mm patterned on a 5mm-thick silicon substrate. Wavefront in 1st-order the diffracted beam error due to pattern distortion was measured at $\sim \lambda/40$.

SUMMARY

LumArray's ZP-150 zone-plate array lithography tool has the capability to produce high quality CGH devices for metrology of aspheric and other complex surfaces. Global pattern distortion of <30 nm has been demonstrated and is expected to be reach sub-10nm with additional measurement and calibration. The ability to pattern on thick, optically flat substrates removes substrate flatness as a common source of error in the application of CGH-based interferometry. High patterning speed can lower cost and lead-time barriers to wider adoption of CGH's in commercial applications.

ACKNOWLEDGEMENTS

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TACTILE COORDINATE METROLOGY FOR ULTRA-PRECISION MEASUREMENT OF OPTICS: RESULTS AND INTERCOMPARISON

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INTRODUCTION

Coordinate Measuring Machines (CMMs) are widely used for 3D metrology of parts in science and industry. The Isara 400 3D CMM, developed by IBS Precision Engineering, advances coordinate metrology into ultra-precision accuracy, offering a flexible solution for full 3D measurement of a wide range of (large scale) products, with accuracy at nanometer level: an expanded measuring uncertainty (calculated according to GUM [1]) of 57 nm (2σ) in 1D and 109 nm (2σ) in full 3D is achieved in a measuring volume of 400 x 400 x 100 mm, which is unprecedented in its field. The design, realization and calibration of the Isara 400 were presented at the 2011 ISMTII [2]. Since then, extensive research has continued for various measurement applications; the measuring capabilities of this machine have been successfully applied in several demanding measurement tasks [3].

One of the most interesting applications is the measurement of complex optical elements such as aspheres and freeforms; tactile coordinate metrology enables measurement of optics with steep slopes (up to 90°), which is often problematic for optical measurement techniques. Manufacturing of optical surfaces has steadily improved and with advanced techniques the

removal of material at nanometer level is possible. To exploit this possibility, measurement techniques must be available for measuring the form of strongly curved optics with an accuracy of a few tens of nanometers. Also, when high accuracy measurement of the optical surface is used as input for (re-)machining specific areas with remaining form errors, there is a need for referencing the measurement to a work piece coordinate system. Tactile 3D metrology enables to reference the optical surface to many types of reference features, even when these are not located on the optical surface itself (e.g. side edges).

In the following sections, measurements of multiple artefacts are presented, demonstrating the measurement capabilities of ultra-precision 3D coordinate metrology. First a comparison of a flat mirror compared to optical calibration by the PTB is shown to verify the (traceable) measuring uncertainty of the Isara 400. In addition, the results of two round robin artefacts are presented, which were used for an extensive intercomparison between various measurement systems. The first is a large ($\varnothing 380$ mm) aspherical telescope lens, the second a small polymer coated lens with a clear aperture of about 12 mm.

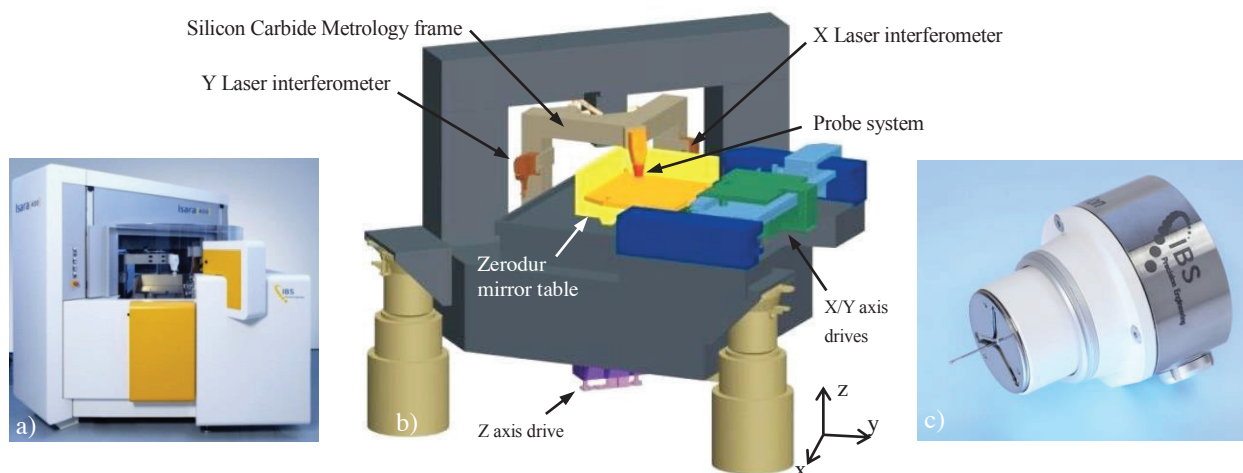


FIGURE 1: a) Photograph of the Isara 400 3D CMM; b) 3D design concept; c) close-up of the "Triskelion" tactile probe.

Figure 1 shows an overview of the Isara 400 3D CMM. Three plane mirror laser interferometers are applied as measuring systems for the machine axes. The interferometers each measure against the sides of a monolithic Zerodur mirror table, on which the work piece is mounted. These interferometers are mounted in a single body metrology frame, which also holds the probe system. The laser beams are aligned to the probe tip and their mutual alignment does not change during movement of the axes, thus fulfilling the Abbe principle in 3D within the complete measuring volume. As a result, straightness errors and rotations of the three translation stages will have no first order influence on the measurement result.

1. VERIFICATION OF MACHINE ACCURACY: FLATNESS MEASUREMENT

The calibration procedure of the Isara 400, consisting of an accurate correction for flatness and out-of-squareness errors of the mirror table, is described in [2]. In order to verify the accuracy of the calibrated measuring machine, various reference artefacts are measured. One artefact which is used is an Ø150 mm Zerodur flat mirror. The flatness of this mirror was measured at Germany's national metrology institute, the Physikalisch-Technische Bundesanstalt (PTB), using Fizeau interferometry, which is directly traceable to the international standards.

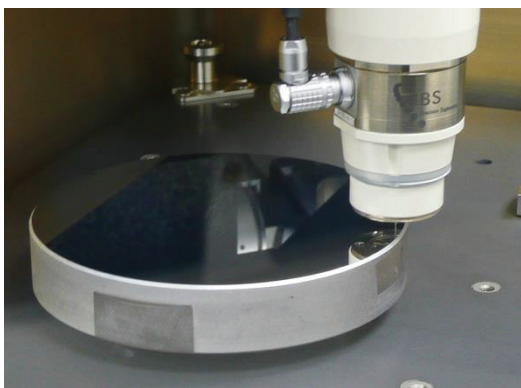


FIGURE 2: Measurement of the Ø150 mm Zerodur flat mirror in the Isara 400 CMM.

This mirror is measured on the Isara 400 CMM; using a tactile probe ("Triskelion A-250" with Ø0.5 mm tip [4]), a grid of 4400 measurement points was measured on the mirror surface (see figure 2).

The measurement data from the Isara 400 CMM is compared to the interferometric measurement from the PTB.

The difference between the Isara 400 measurement and the PTB calibration is determined by subtraction (see figure 3). The residual of this subtraction is shown as a surface plot and also as a histogram, which shows the distribution of the residual. It is found that 95.5% of the data points match to less than 11 nm with the PTB calibration.

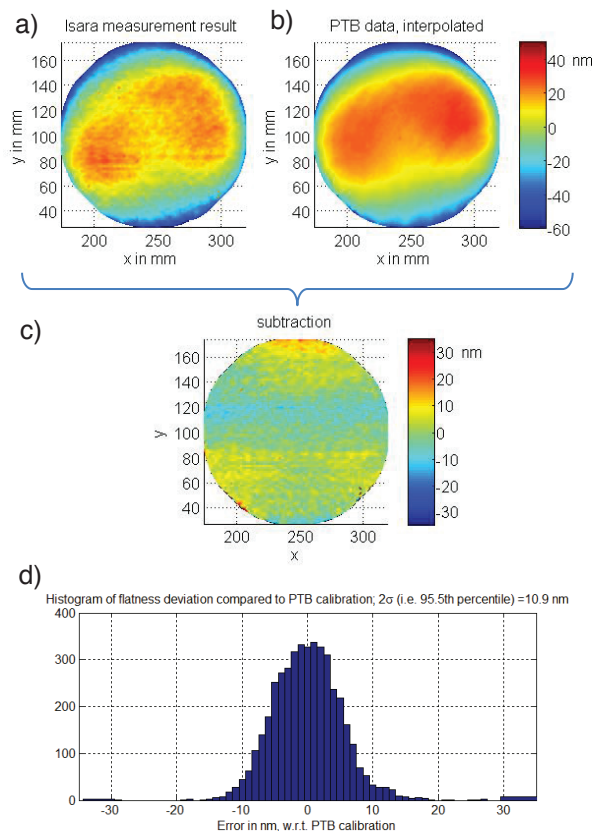


FIGURE 3: a) Flatness measurement result from Isara 400; b) PTB calibration; c) Comparison; d) histogram of comparison.

In addition to point measurements, it is also possible to perform tactile scanning measurements, using the same Triskelion probe. During the scan, the deflection of the probe is used as feedback to maintain contact with the work piece; no prior knowledge of the surface is required. Figure 4 shows the result of such scanning measurements across the center of the flat mirror (measurement time 4 minutes, 600.000 data points in profile). The graph shows the form error along this line, for two scan

measurements superimposed on the PTB calibration data. Again, this result indicates that the Isara 400 is capable of matching the optical calibration measurements to approximately 10 nm.

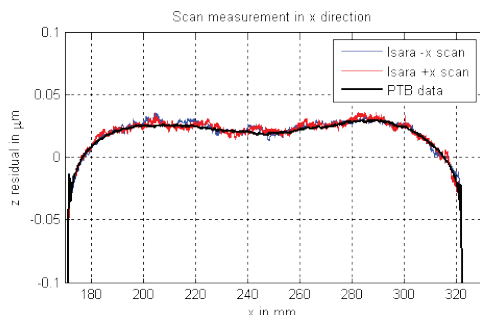


FIGURE 4: Result of tactile scanning measurement across center of flat mirror.

2. MEASUREMENT OF ASPHERIC LENS FOR THE VERY LARGE TELESCOPE

The European Metrology Research Program (EMRP) IND10 “Optical and tactile metrology for absolute form characterization” is a joint research project, aiming to improve form metrology which is urgently needed for modern optical surface production. This project brings together experts from optical metrology as well as from coordinate metrology.

Within this EMRP project, extensive intercomparison measurements on optical artefacts have been performed. One such artefact is an Ø380 mm aspheric lens. This lens was developed for the Very Large Telescope (VLT) in Chile as part of an instrument for projecting artificial stars to compensate for atmosphere turbulence and has been manufactured by the Dutch research organisation TNO. Such lenses are manufactured in an iterative loop of measurement and re-polishing to obtain an end product with less than 25 nm RMS form deviation [5]. This particular lens was rejected in an early stage of production and will therefore show higher form deviation.

Due to the dimensions and accuracy requirements of this lens, it is challenging to measure using conventional measuring techniques; for example, measurement systems applying conventional interferometric surface metrology are not available for products of this size, given that it would be required to stitch together tens of measurements from smaller fields-of-view to obtain a full surface map, which

significantly reduces the overall accuracy of the measurement. In the field of tactile metrology, most measurement systems also cannot accommodate products of this size. Within the EMRP project, the intercomparison was limited to the Isara 400 CMM and the NANOMEFOS [6], a non-contact measuring machine specifically designed for measurement of aspheres and freeform, making use of an optical probe system.

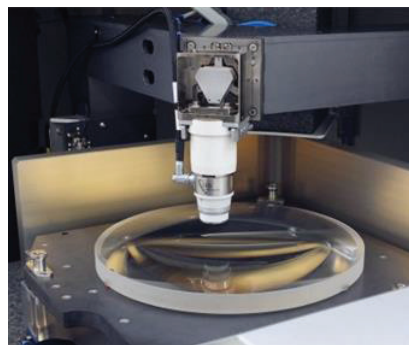


FIGURE 5: Measurement setup of the Ø380 mm aspheric lens on the Isara 400 3D CMM.

The nominal form of the lens is prescribed by the aspheric formula (Eq. 1) and the parameters from Table 2.

$$Z_{sag} = \frac{r^2}{R_0 \left(\sqrt{\left(\frac{1-r^2(K+1)}{R_0^2} \right) + 1} \right)} + A_2 r^2 \dots + A_{10} r^{10} \quad (1)$$

TABLE 1: Parameters of the VLT Asphere

Lens Type	Glass
Clear Aperture	300 mm
Radius (R_0)	637.38 mm
Conical constant (K)	-0.4776
Aspheric constants	$A_2 \dots A_{10} = 0$

To measure the lens on the Isara 400 (see Figure 5), the CAD model of the lens was imported in the CMM software. A few points were measured by manual control and a (coarse) alignment was determined. Next an automated measurement grid was defined and measured and the new best fit alignment was evaluated. The final measurement of this large lens was performed using a point-to-point grid measurement of 4800 points.

During processing of the data, a best-fit alignment was determined using least-squares

optimization, and the resulting form errors were then calculated by subtracting the nominal form. Figure 6 shows the form error of the asphere as measured with the Isara 400 CMM. This measurement was executed twice and the differences between measurement 1 and 2 are shown on the right hand side. On the bottom right a histogram shows that the repeatability of these two measurements is less than 13 nm (2σ).

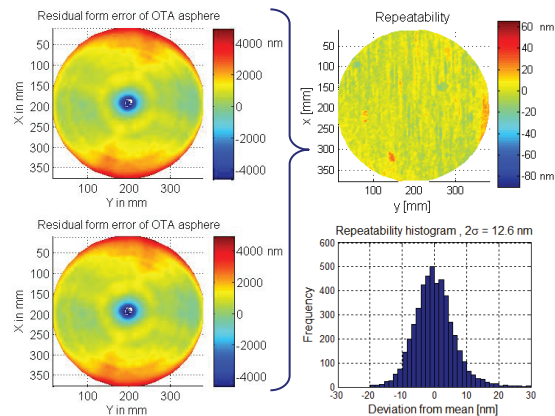


FIGURE 6: Repeatability of the Ø380 mm aspheric lens measurement on the Isara 400 CMM; two measurement results (left), residual after comparison (right). The histogram shows the repeatability of 13 nm (2σ).

Figure 7 shows the measurement results for the same lens performed by TNO using the NANOMEFOS machine and the measurement result obtained from IBS Precision Engineering's Isara 400 CMM, using the same color scales and plotting styles. Good comparability between these different measurement methods can be observed.

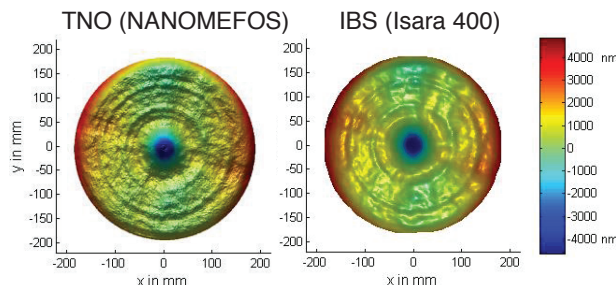


FIGURE 7: Measurement results of TNO using the NANOMEFOS (left) and IBS using the Isara 400 CMM (right)

The resulting Peak-to-Valley values and RMS form error of the two measurements are listed in table 3 and it can be seen that RMS values match within 30 nm. The PV-values match within 150 nm, which is only 1.5% of the PV form errors. The difference in PV value can thus be explained by the differences in point cloud distribution; the NANOMEFOS performs continuous scans along concentric circles, whereas the Isara 400 measurement employed a uniform grid of points. Given the relatively large form errors (almost 10 μm PV) of this reject lens, a different distribution of measurement points directly affects the obtained PV value.

TABLE 2: Measurement results OTA lens

Measurement	PV value	RMS value
TNO (NANOMEFOS)	9673 nm	1021 nm
IBS (Isara 400)	9523 nm	1052 nm

3. SMALL POLYMER LENS MEASUREMENT

Another artefact which is part of the intercomparison measurements within the EMRP project is a small polymer coated lens, which has been made available by Anteryon. The transparent polymer coating is applied onto the base material, making it difficult to measure for many types of optical probes, as it will experience a double reflection.

This lens has been measured by several partners in the project, including IBS on the Isara 400 CMM, the Dutch metrology institute VSL on a Zeiss F25 CMM [7,8], the Swiss metrology institute METAS, using a small interferometer based microCMM [9,10] and the Institut für Technische Optik (ITO) of the University of Stuttgart, Germany using a Tilted-Wave-Interferometer (TWI) [11].

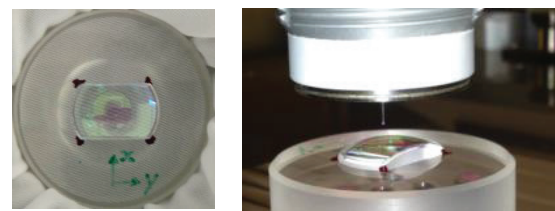


FIGURE 8: Polymer lens (left) and measurement setup on the Isara 400 (right).

Figure 8 shows the polymer lens on the left and the measurement setup in the Isara 400 CMM on the right. Table 3 lists the details of the polymer lens.

TABLE 3: Details of the polymer lens artefact

Lens Type	Polymer Coated Asphere
Clear Aperture	11.74 mm
Radius (R_0)	$1 \cdot 10^{20}$ mm
Conical constant (K)	-1
Aspheric constants	$A_2 \dots A_{10} \neq 0$

The form error is calculated in the same way as described in the previous paragraph, with the asphere formula and the parameters from Table 3 providing the nominal form of the lens. Figure 9 shows the form error as measured on the Isara 400 CMM; the clear aperture is indicated by the dashed circle. This area was re-measured using a fine grid spacing of 0.2 mm.

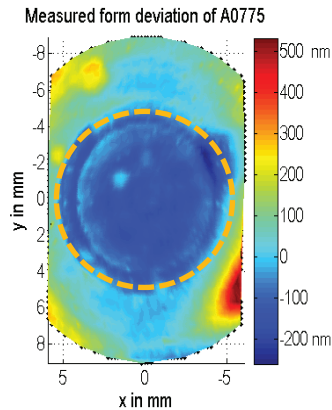


FIGURE 9: Measurement result of the full lens surface, showing the clear aperture

The results of IBS (Isara 400 CMM), VSL (Zeiss F25), METAS (microCMM) and ITO (TWI) are shown in figure 10. This data was evaluated by the Physikalisch-Technische Bundesanstalt (PTB), to ensure uniform data processing. The data points are shown as squares, with the size of the square indicating the grid spacing; finer grid spacing results in more data points and thus smaller squares in the plot. Some plots show small white areas, which are removed data points (for example outliers due to contamination).

It can be observed that for every measurement method, different grid spacing was used. In some cases, this is due to the nature of the measurement; the optical interferometry based measurement from ITO receives pixel information, while the tactile methods of IBS and VSL use physical point measurements. The METAS machine used scanning lines over the surface and therefore has a smaller grid spacing in y-direction compared to the x-direction. Nonetheless, good similarity in measurement results was obtained between these four measuring systems; the results are summarized in table 5, which lists the PV-values and RMS form error for every measurement.

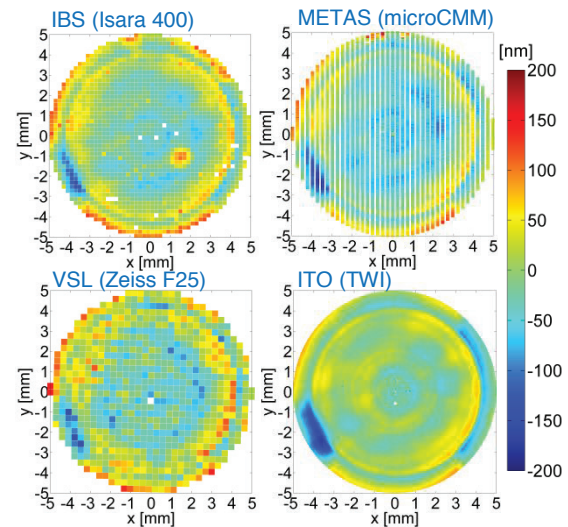


FIGURE 10: Form error results of the polymer coated aspheric lens within a Ø10 mm aperture.

TABLE 4: Intercomparison results for the Polymer lens measurement

Participant	PV value	RMS value
IBS (Isara 400)	270 nm	42 nm
METAS (microCMM)	419 nm	31 nm
VSL (Zeiss F25)	292 nm	46 nm
ITO (TWI)	286 nm	40 nm

4. CONCLUSIONS

Various measurement applications have been presented to illustrate versatility and measurement capabilities which is offered by the Isara 400 ultra-precision CMM. A flat mirror

verification measurement showed traceable measurement errors as low as 11 nm.

The measurement of the Very Large Telescope asphere (Ø380 mm) requires almost the full measurement range of the Isara 400. The results are shown to be repeatable within 13 nm. Comparing the measured form deviation to a measurement by TNO on the NANOMEFOS machine, RMS values show good comparability within 30 nm, showing that both machines offer an innovative solution for the demanding measurement task.

For the intercomparison measurements of a second artifact, a polymer coated aspheric lens, was measured on a variety of measurement machines. RMS form error values of the optical surface match to within 15 nm between the four measurement techniques applied in this intercomparison. Three partners have applied a tactile measurement using different types of 'micro CMMs', showing that this technology is mature and reliable for the measurement of high precision optics.

5. ACKNOWLEDGEMENTS

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Finally we want to give special thanks to Gernot Blobel of the PTB for his efforts to do the data analysis and comparison for the Polymer lens measurement.

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MULTI-SCALE CALCULATION OF CURVATURES FROM AN ASPHERIC LENS PROFILE USING HERON'S FORMULA

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INTRODUCTION

The objective of this work is to demonstrate the use of Heron's formula to perform a multi-scale analysis of curvatures on profiles measured on an aspheric lens. Also included are analyses of juncture regions created by different stitching of adjacent profile measurements. The curvatures are analyzed as a function of position and scale. In the context of this work, the validity of basic principles surface metrology [1] are also discussed.

The use of multi-curvature analysis for profiles has the potential to improve quality assessment and process control for aspheric lenses. The use of curvature analysis can be used for critical evaluation of stitching algorithms

Multi-scale curvature analysis also should be useful for characterizing profiles to identify functional correlations. Curvature has been used in modeling contact mechanics and stress concentrations. Like slope, of which curvature is an approximate spatial derivative [2], curvature can vary with the scale of observation, where the scale can be considered to be the length of the region over which the curvature is calculated (Fig. 1). Curvature can also vary with position along the profile. Part of the attractiveness of curvature as a characterization parameter is that it does not depend on a datum.

Traditionally, estimations of curvatures have used best-fit algorithms applied to measured profiles. The curvatures resulting from this analysis depend on the number of measured height samples used in the fitting exercise and on their nominal horizontal spacing. Usually, in measured profiles the sampling interval, i.e., the horizontal interval between each of the measured height samples, is constant. When this is the case, then the number of measured

height samples, considered in a best-fit estimation of the curvature, is coupled with the scale of the estimation. A traditional procedure for characterizing the curvature can be based on selecting a certain sized region, or scale, for the estimation. There could be several bases for the selection of the region; for example, it could be a region where the curvature appears to be approximately constant. This selection can introduce subjectivity. A best-fit algorithm then might be used to characterize the curvature over this region. The change of curvature on the edge of the region has been handled by estimating the angle of the departure from the best fit [3]. In this kind of procedure, nothing is learned about any variation of the curvature regarding position and with scale.

The scale of the curvature would be important to the kind of interactions it might influence or be influenced by. Deviations from the intended curvatures at larger scales would impact focusing and, at finer scales, often associated with the scales of roughness irregularities, could cause scattering of the light that the lens is intended to focus. Both of these kinds of deviations could be detectable by curvature estimations, albeit at different scales.

Samper and LeGoïc [4] recently presented a method for calculating curvature tensors, based on taking the second spatial derivative, after using a modal representation of the measurement. This representation can be robust with respect to outliers and boundaries and has the potential to adapt to multi-scale characterizations, perhaps by changing the number of nodes used in the representation.

The calculation of curvature based on Heron's formula always relies on three measured heights, regardless of the scale. Heron's

formula has been described previously for characterizing curvatures on profiles extracted from surface measurements of the edge of a biopsy needle [5] and the edge an aluminum alloy that is subject to mass finishing [6]. Curvatures can also be calculated from measured profiles using finite difference approximations for the derivative of the slope [2] and by using the well-known method from classic calculus. The disadvantage of the latter two methods is that curvatures that they calculate are not constant over the interval representing the scale. The disadvantage of calculating the curvature by Heron's formula is that when the radius is greater than twice the distance between the two external height samples Heron's formula underestimates the curvature. This situation usually only occurs at fine scales on rough surfaces [5,6].

Heron's formula has recently been used to find a strong correlation ($R^2=0.96$) between surface curvature and the fatigue limit for steel with surfaces created by ball-end milling [7]. The strongest correlation was only found at a specific scale, about $610\mu\text{m}$. At scales just above and just below this, the strength of correlation R^2 falls to less than 0.5.

Previously, the multi-scale calculation of curvatures versus position was applied to surfaces with a relatively large amount of irregularity. In this work, the curvature calculations are applied to relatively smoother profiles that have been stitched. The exercise is used to test the capacity of curvatures calculated in this manner in order to detect small amounts of irregularity at a scale where the profile appears to be smooth. The scale specificity of this detection is discussed in the broader context of the importance of scale and multi-scale representations of geometric properties on measured textures.

METHODS

Heron's formula for curvature calculation

To calculate curvature as a function of position and scale, Heron's formula (Fig. 1) is used to determine the curvature of a circumscribed circle that includes three height samples from a measured profile. The nominal horizontal spacing between the two exterior height samples represents the scale.

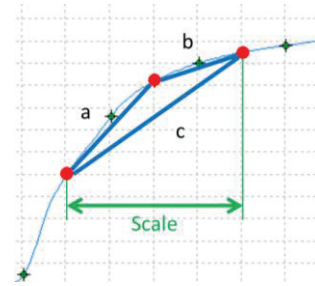


FIGURE 1. Calculation of curvature using Heron's formula. The scale of the calculation is indicated. The light blue line indicates a cartoon profile, and the circle/cross symbol represents measured height samples. There is only one finer scale for calculating the curvature with this sampling interval.

Heron's formula uses three equations based on the lengths of the sides of the triangle (a , b , c), with the positions of three measured heights as its vertices, to calculate the curvature:

$$k=4w/(a+b+c) \quad (1)$$

where k is the curvature and w is given in equation 2:

$$w=(s(s-a)(s-b)(s-c))^{0.5} \quad (2)$$

and s is given in equation 3 as:

$$s=(a+b+c)/2 \quad (3)$$

In all the calculations, the same number of sampling intervals separated the measured height samples.

Profile Measurement and Stitching

Two overlapping profiles were measured, using a Taylor Hobson PGI surface profilometer with a 5 micron radius diamond stylus. The lens is made from nickel-plated stainless steel. The part was diamond turned with a feed of 150nm per revolution on a Nanotech 250 ultra-precision lathe.

Stitching of the two measured profiles was done in two ways. One used an algorithm that minimized the difference in position in the overlap region, using horizontal and vertical translations and without averaging in the overlap region. The other process, employing a commercial software, Mountains7® (Digital Surf), uses translation, rotation, and averaging in the overlap region, and lets the user select the

size of the overlap region. Three overlap sizes are investigated.

RESULTS

The profile after the stitching with just translation is shown in Fig. 2. The profile looks appealingly smooth. All of the stitch profiles, including those done by the commercial software, appear to be indistinguishable.

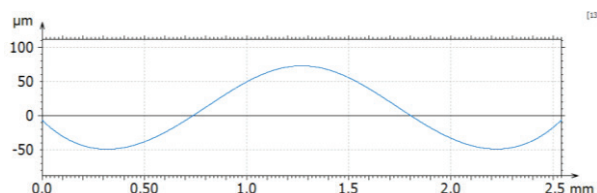


FIGURE 2. A profile from an aspheric lens mold, stitched using a translation stitching algorithm and cropped to focus on the lens.

Results of the curvature analysis are plotted in a surface of curvature versus position and scale (Fig. 3). At the largest scale, that of the total stitched profile, only one curvature can be calculated, and this is only at the central position. It uses the height samples at the two ends of the profile and in the middle for the calculation. This plot transforms the profile into a surface within a curvature-position-scale space. A curvature-position section of this curvature-position-scale surface at the appropriate scale looks much like the inverse of the profile, because the convex and concave sections transform to negative and positive curvatures, respectively.

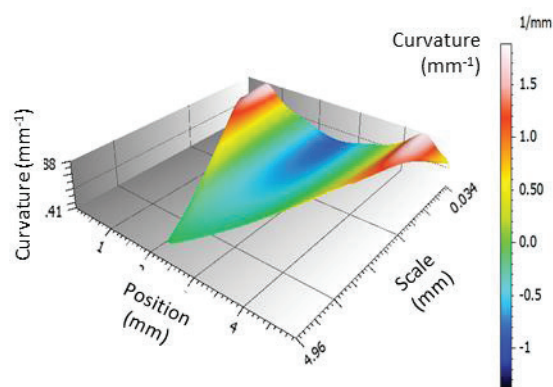


FIGURE 3. A transformation of the profile in Fig. 2 to a surface in a curvature-position-scale space. Fine scales are eliminated to focus on the form.

A curvature-position section at a scale of $40\mu\text{m}$ is shown in Fig. 4. The negative curvature at the center of the lens is clear, as are the positive curvatures to the sides. At the edge of the lens, just before the curvature becomes negative, irregularities are obvious. This is the region where the stitching has occurred.

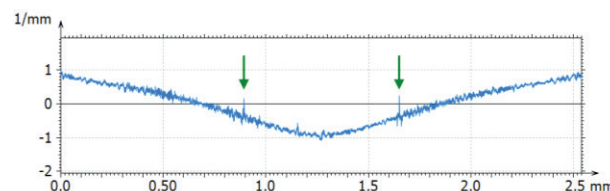


FIGURE 4. Noncommercial software stitching of a curvature-position section at a scale of $20\mu\text{m}$. Stitching irregularities are indicated.

Analyses of curvature-position plots from three stitching attempts in the commercial software follow. All are at a scale of $20\mu\text{m}$.

In Fig. 5, the overlaps from stitching with an overlap of $600\mu\text{m}$ are clearly distinguishable by upward spikes at the approximate positions of 1.06mm and 1.67mm.

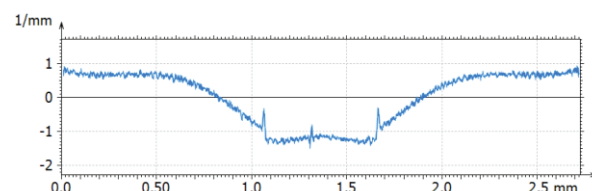


FIGURE 5. Commercial software stitching, $600\mu\text{m}$ overlap, with curvature-position at a scale of $20\mu\text{m}$.

The stitching used for the profile characterized in Fig. 6 is that recommended by the software. The overlap is $791\mu\text{m}$. The overlap regions are not distinguishable from what might be noise.

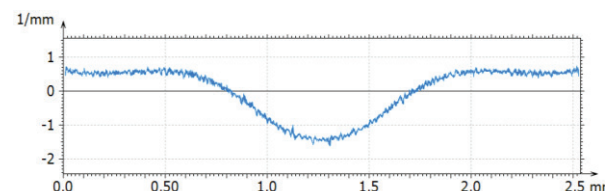


FIGURE 6. Commercial software stitching, using the recommended $791\mu\text{m}$ overlap, with curvature-position at a scale of $20\mu\text{m}$.

In Fig. 7, the overlaps from stitching with an overlap of $900\mu\text{m}$ are clearly distinguishable by

downward spikes, in the direction opposite to that noted with the smaller overlap, extending from positive to negative curvatures at the approximate positions of 0.77mm and 1.67mm.

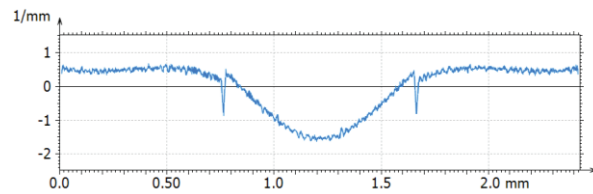


FIGURE 7. Commercial software stitching, at 900µm overlap, with curvature position at a scale of 20µm.

In all three overlap cases using the commercial software, there is crossover from positive to negative at about 0.82mm. With the non-commercial software, this crossover occurs at 0.70mm. The crossover from negative to positive for the commercial software occurs at 1.88mm for the 600µm overlap, 1.72mm for the 791µm, and 1.57mm for the 900µm overlap. This trend is also consistent with the respective lengths of the profiles after stitching. For the noncommercial software, the second crossover is at 1.84mm, comparable to the 600µm overlap, while the total length is most similar to the 791µm overlap.

DISCUSSION

Both methods result in appealingly smooth-looking profiles in the raw dimensional space. The transformation into the curvature-position space at a scale of 20µm clearly shows spikes in the curvatures; these spikes appear to correspond to some position in the stitched region in three out of four cases.

There appears to be what could be called noise, at least for the purpose of the study of stitching, in the representations in the curvature-position space at a scale of 20µm. This noise is one reason to limit the lower scale on the curvature-position-scale plot in Fig. 2 to 34µm, which is focusing on form. Whether this noise is representative of the surface or is an artifact of the measurement would require further study.

Curvature, being approximately the second derivative of the position, should be sensitive to noise in the measurement. At what scale the noise begins to dominate is not clear. The stylus has a curvature of 200mm⁻¹. This is well

removed from the largest curvatures calculated in this study. Therefore, it appears that the stylus tip should not have adversely limited the resolution in this study. The feed per revolution at 150nm is also well removed from the scales in this study and should not have contributed to noise.

Consider what might be called tenets, principles or axioms of metrology [1]. In order to have value, the results of a metrological study should be able to discriminate based on quality. To do this, the following axioms should apply:

1. the measurement must include the appropriate features at an appropriate resolution (i.e., scale); and
2. the appropriate aspect of the geometry must be characterized at the appropriate scale.

This study supports these tenets because it has been possible to discriminate stitching algorithms based on the characterization of curvature at a scale of 20µm. This discrimination was not possible based solely on the profiles. It also was not possible at scales much larger or smaller than 20µm. Clearly, the measurement had the appropriate resolution of the appropriate features, or the discrimination would not have been possible.

CONCLUSIONS

Curvatures calculated by Heron's formula at a scale of 20µm on stitched profile measurements appears to be capable of discriminating the results of several different stitching exercises, algorithms that are indistinguishable on the profiles themselves. These results are consistent with basic tenets of surface metrology.

ACKNOWLEDGEMENTS

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Productive Modes for Ultra-Precision Grinding of Freeform Optics

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INTRODUCTION

There are numerous applications demanding the use of medium to large freeform surfaces and optics. These include short wavelength microlithography, segmented ground based telescopes, compact space based observers, high power laser systems, IR defence systems and even international temperature definition apparatus. The authors have been engaged in developing effective fabrication of freeform surfaces demanded for all of the above mentioned applications [1,2,3,4].

This paper introduces developments carried out that have significantly improved the production method for grinding freeform shape optics. Much of the work carried out has been reported in detail elsewhere, the developments are broadly introduced here and clearly referenced.

BACKGROUND

Numerous optical systems can benefit from application of freeform surfaces. Available performance benefits can include: compact system design, reduced number of optical elements, reduced mass, shorter optical paths and reduced angle of incidence/absorption, naming but a few. These benefits are not unlike those seen in earlier decades when rotationally symmetric aspheric surfaces started to be adopted replacing optical systems formed by spherical shape optics.

Ultra precision generation of freeform optics has been well demonstrated for a number of years using the diamond turning process in a mode called slow slide servo technique [5]. However, limitations of diamond turning restrict its application to a narrow range of metals and crystals. Its application for producing optics made in glass and ceramics has not yet proved to be effective. Consequently, the generation of optical surfaces in glass and ceramics has been predominantly performed using fixed abrasive grinding prior to subsequent polishing and

figuring processes. Ultra precision machines employing purely positional controlled surface generation techniques typically can achieve form accuracy to size in the range of 1 part in 10^6 . Meaning a 1 metre surface can be machined to a form accuracy of 0.001mm (1 micrometre RMS). Achieving beyond this relative level of size to form accuracy has not yet been reported at any notable level of production output.

BOX MACHINE

In 2004, the authors embarked on designing and building a large scale freeform optics grinding machine, see Figure 1. The BoX[®] machine was conceived to be able to grind freeform optical surfaces of up to 2 metres in diameter, achieve 1 part in 10^6 form accuracy / size ratio and have a production rate of 1 metre² per 10 hours removing a minimum depth of 1 mm. Achievement of the above goal has been reported in detail [6].



FIGURE 1. BoX[®] Freeform grinding machine

Significant data regarding the associated levels of induced sub-surface damage has also been presented in scientific and academic journals [2,3]. This sub-surface analysis linked the machine dynamic performance to the induced

level of sub-surface damage at given rates of material removal.

The BoX[®] machine design is based on a simple 3 axes cylindrical co-ordinate motion system employing a toric shaped grinding wheel and specially defined optical tool path software. The tool path software adjusts the motional path of the machine linear axes to enable accurate freeform surfaces to be effectively produced. This motional tool path adjusts the contact point of the wheel with respect to the freeform surfaces moving the work to wheel contact point in 2 directions around the toric shape wheel, see Figure 2.

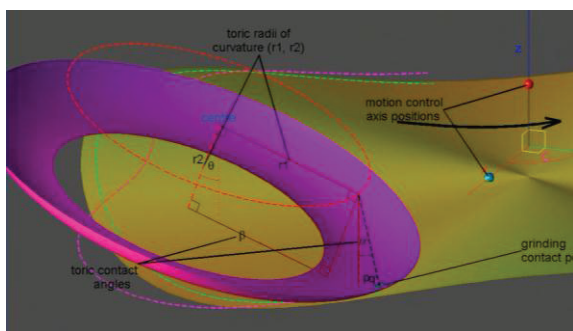


FIGURE 2. BoX[®] Tool path control method

The key benefits of this simple 3 axes cylindrical co-ordinate machine design can be summarised as:

- Low number of machine interfaces (bearings) offering achievement of high static loop stiffness
- Limited level of moving mass maximising dynamic stiffness
- Eased application of large area hydrostatic bearings maximising smoothness, load capacity whilst minimising vibration levels at high load levels
- Simple 2 plane symmetric machine design that minimises tilts errors caused by any thermal distortion from high power sub-systems

E-ELT PRIMARY MIRROR SEGMENT

The capability of the BoX[®] freeform grinding approach was clearly demonstrated in producing 1.46 metre sized freeform mirror segments for the ESO E-ELT [10] see Figure 3. These

freeform shaped primary mirror segments, made in both Corning ULE[®] and Schott Zerodur[®], were ground using the BoX[®] machine

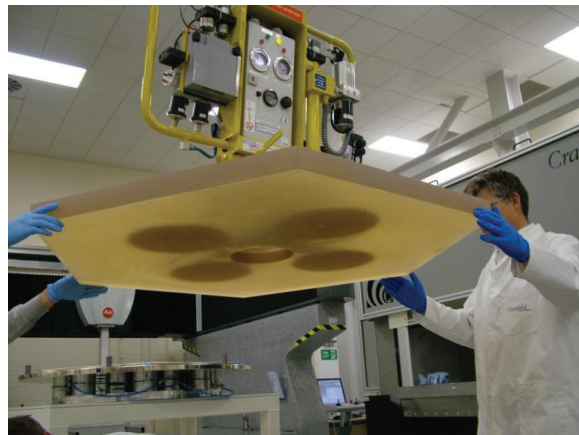


FIGURE 3. E-ELT primary mirror segment

The achieved level of form accuracy was better than 1 micrometre RMS, see Figure 4. The processing duration was below 20 hours (equivalent to 10 hours per m²).

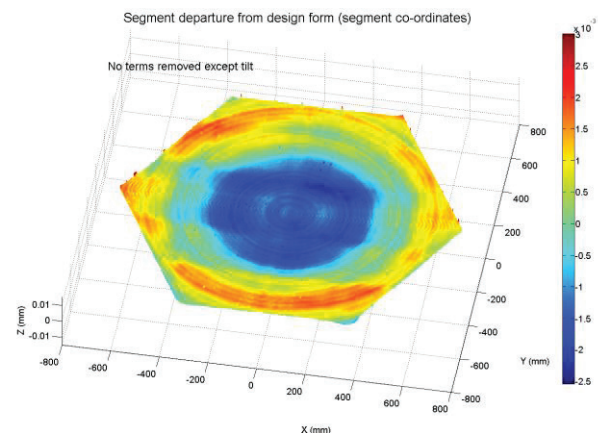


FIGURE 4. Form accuracy E-ELT primary mirror segment: 0.6 μ m RMS from Leitz pmmf 30.20.10

Whilst the E-ELT mirrors represent freeform surfaces their departure from being rotationally symmetric is only 0.15mm. Nevertheless the achievement of 0.6 micrometre RMS (4.8 micrometres P-V) measured with 580,00 data points represents a significant achievement given the rapid processing rate.

DECENTRED SPHERE GENERATION

Proving the performance of a diamond turning machine for producing freeform surfaces has been demonstrated by machine suppliers through the generation of a so-called decentred sphere. The consideration being that the

spherical surface is easy to measure yet the machine motion demands to produce a decentred spherical surface significant but also easily understood.

Consequently, the BoX[®] machine performed the same decentred spheric generation task to prove its functionality. A 1 metre radius of curvature spherical surface is rapidly ground in a decentred mode. The offset distance of 13mm yields a maximum acceleration/deceleration of the linear axes of 20mm/second² at a processing rate of 1 metre² per 10 hours.

Critically, in this dynamic grinding mode, is the level of induced sub-surface damage. Investigation of ground decentred spherical surfaces was undertaken. Ground surfaces of this type were robotically polished in the regions that equated to the surface areas of minor and major acceleration/deceleration, See Figure 5.

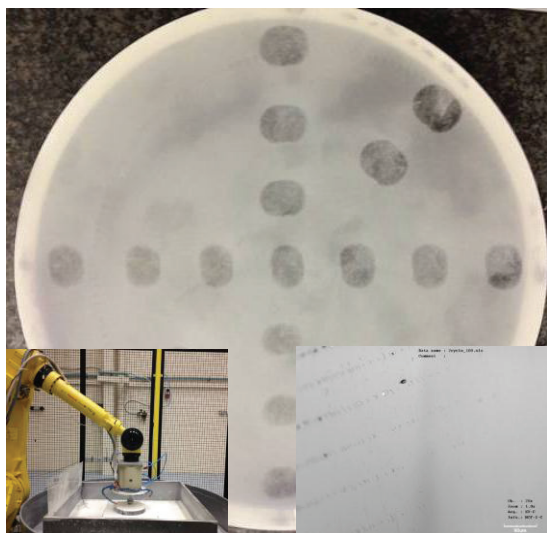


FIGURE 5. SSD evaluation of decentred sphere

The insets of Figure 5 show: (left) Cranfield's robot polishing system, (right) an optical micrograph showing worst case sub surface damage disappears at 14 micrometres below the surface of this decentred ground substrate..

NIF WEDGE LENS

An interesting lens demanded within the LLNL National Ignition Facility is a so-called wedge focussing lens. This lens has a freeform surface notably tilted with respect its second surface. Typically its freeform surface would be generated using a raster grinding process offered by a 3 linear axes Cartesian frame grinding machine. In the case of the BoX[®]

machine, 4 of these 400mm square shape NiF lenses can be ground in a single set up as depicted in Figure 6. A 3.5 hour production grinding time can be achieved for the freeform surface of these high value lenses.

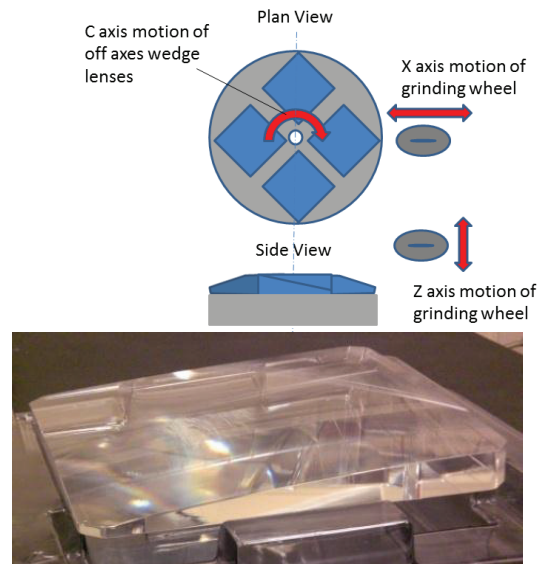


FIGURE 6. (above) Multiple mode grinding for NiF wedge lenses, (below) NiF wedge focus lens

LIGHTWEIGHT SPACE MIRRORS

The demand for lightweight space optics is increasing with greater complexity of the shape and substrate format. Reduced mass for a given level of substrate rigidity is a continuous development activity. This trend has brought about a shift to advanced ceramic substrates made for example, variants of silicon carbide.



FIGURE 7. Light weight silicon carbide mirror substrate produced by Boostec of France

The trend to a reduced mass leads to thinner and thinner shell and support substrate structures. A consequence is that mirror grinding

hugely benefits from a high responsiveness of the tool point. This high dynamic control of the tool point enables grinding passes to be made with correction for the differing deflection seen within each substrate shell structure. Figure 7 shows an advanced silicon carbide space optic substrate produced by the Boostec company of France.

By applying the BoX[®] grinding process, with tool path error correction for regional substrate deflection, higher quality form accuracy can be achieved with minimised processing times. Figure 8 illustrates the form accuracy and surface structure after BoX[®] grinding of the Boostec 600mm diameter very thin and highly light weighted silicon carbide substrate.

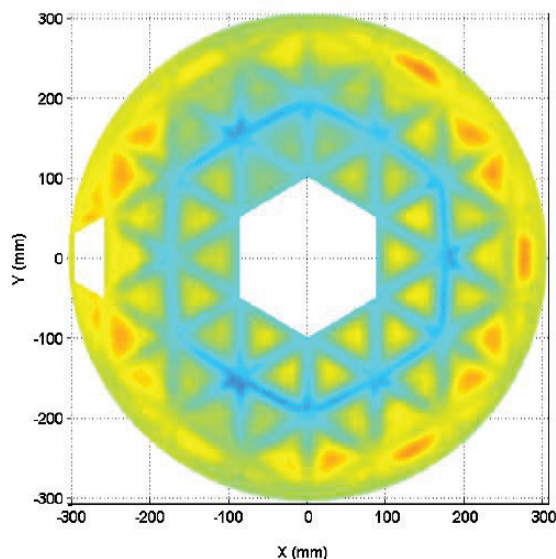


FIGURE 8. Minimising “print through” of space optics by dynamic tool path control

The achievement of improved form accuracy significantly alleviates time spent during subsequent polishing and reactive plasma figure corrections. These finishing process steps typically represent the major bottleneck processing operations, reducing their duration by advanced grinding represents a significant production cost saving opportunity. Figure 9 shows the finish ground thin shell silicon carbide mirror.

CONCLUSIONS

This paper has introduced the development of a number of freeform grinding modes. These modes are shown to be effective through high power and high dynamic machine motional performance. High dynamic response is gained

by the BoX[®] grinding machines simple, low moving mass, 3 axes cylindrical co-ordinate basis. The slow slide servo technique previously applied to diamond turning machines is proved to be highly functional in a high power grinding machine. Low levels of sub-surface damage have been linked to machine tool dynamic loop stiffness. Multi component processing is shown to reduce grinding times. High quality form accuracy, even for thin section substrates, has been demonstrated by application of error correction techniques.

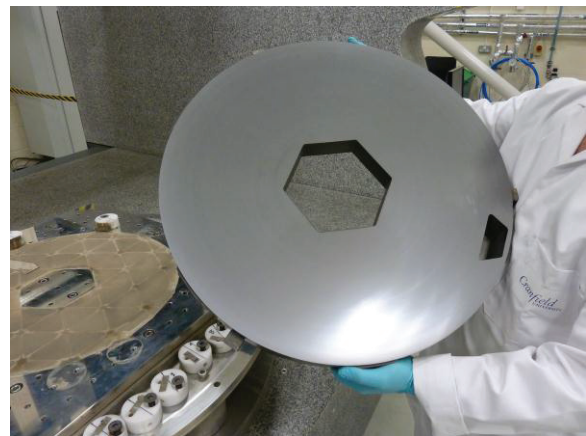


FIGURE 9. BoX[®] ground Boostec light weighted silicon carbide space mirror

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THE DESIGN AND DEVELOPMENT OF A NOVEL 'TWIN TURRET' MACHINING PLATFORM.

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INTRODUCTION

Cranfield Precision has developed a new concept for high precision machining. The classical orthogonal machine tool platform is capable of supporting traditional material removal processes. More recently, ideas such as the Hexapod and the Tetraform [1] have demonstrated benefits for some processes and in the case of the Hexapod style machines, even to find applications outside metal removal in low inertia, high speed metrology [3]. The concept described here leverages capabilities intrinsic to modern control systems to coordinate two rotary axes and a short linear axis in a 'Twin Turret' design which provides a common platform for multiple machine configurations.

MACHINE DESIGN CONCEPT

Cranfield Precision was challenged to find a radical machine configuration that could deliver a highly stiff, thermally stable foundation for a wide range of machine systems, particularly the optics industry for grinding spheric, aspheric and free-form surfaces. Cranfield Precision's experience in the optic industry over many years shows that ductile regime grinding [3] delivers considerable process advantages over conventional glass grinding. This mode of operation is difficult to support unless the machine system is of the highest quality.

The machine uses a unique combination of rotary and linear axes to produce relative motion (both position and angle) between tool and workpiece over a swept working area.

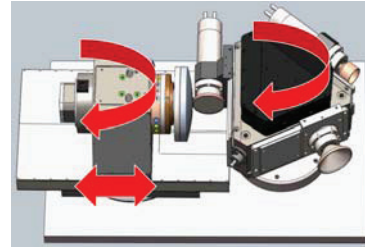


Figure 1: Machine axes concept.

The two rotary axes are rigidly mounted a fixed center distance apart from each other. In order to traverse the tool across components of up to 400mm diameter, the rotary axes need only to rotate by around 20° each and it is possible to align the 'best' segment of the rotary encoders to the critical 20° of motion. It has thus been demonstrated that by using rotary encoders specified with 1 arc sec accuracy over 360° it is possible to reduce the effective error over the 20° working range to 0.1 – 0.2 arc sec, resulting in an uncorrected traverse position error between tool and component of around 0.6µm.

The linear axis is used to control the depth of cut and profile shape of the component being machined. It is a simple procedure during machine build to error correct the linear infeed axis such that the correct profile is followed. It is only necessary to carry out the error correction in one location between the two rotary axes (see fig 2). This is because the center distance between the turrets is fixed and the linear axis is always perpendicular to the 'virtual' linear axis being corrected.

The correction data is thus valid for any position of the linear axis. Fig 2 shows how a Zerodur straight edge can be used to error correct the interpolated straight line motion between tool (here the probe) and component (here the

straight edge). The intrinsic error before correction was in this case $200\mu\text{m}$ and increases as the two rotary axes move towards the opposite extremes of their 20° travel range. After error correction, the straight line motion error was reduced to $0.3\mu\text{m}$.



Figure 2: Proof of concept test machine straightness calibration set up.

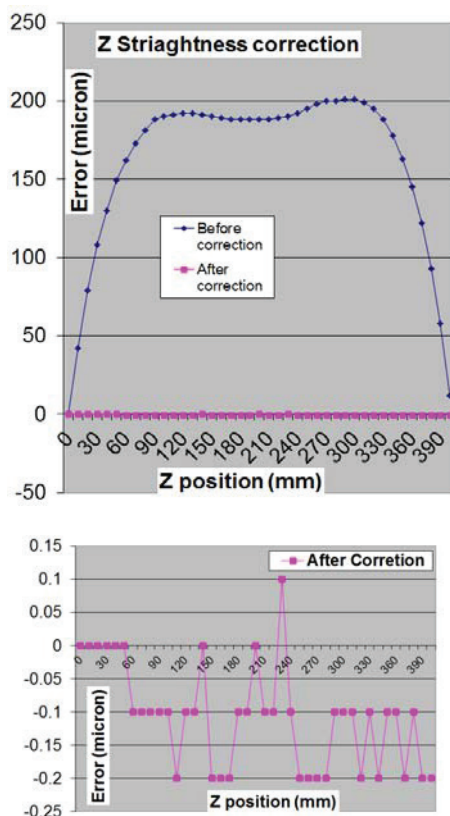


Figure 3: Proof of concept machine straightness, before and after error correction.

Thermal stability

This is a major problem for all machine tools. Conventional grinding machines have large, variable thermal loops that can produce significant movement between wheel and component.

The time taken to grind optical quality surfaces rises exponentially as the specifications for form error, surface finish and sub-surface damage become more challenging and increase part processing time. Larger parts can take days to finish. Such demands dictate a total focus on the maintenance of thermal stability in the presence of coolant, process and ambient thermal influences.

The traditional machine design suffers from a constantly changing coolant return path as the grinding wheel carriage moves along the linear axis. The heat from the grinding process is transferred to different sections of the machine bed, resulting in constantly variable machine distortions. The Twin Turret design enables a simple non-contacting labyrinth seal, making the machine base almost immune to such distortions.

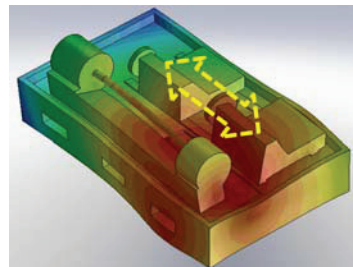


Figure 4: Conventional machine thermal distortion.

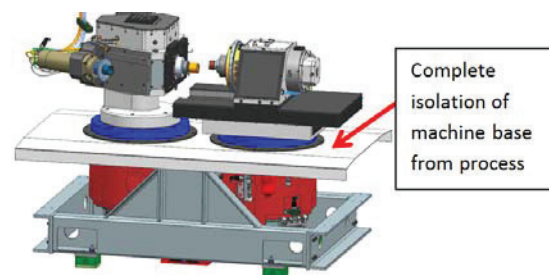


Figure 5: Twin Turret machine base isolation.

Loop stiffness

In conventional machines using stacked linear axes, the bearing interfaces are more compliant than the machine's base material. The target was to use the minimum number of bearing interfaces in the machining loop, thus minimizing compliance. In the optics industry, a common requirement is to achieve 'tool normal' operation of the grinding wheel. A point is selected in the wheel tip which is considered to be a datum that is locked to the cutting point on the workpiece. This method ensures surface texture consistency and reducing the impact of grinding wheel form errors.

Achieving tool normal operation is conventionally achieved by mounting the wheel spindles upon rotary axes that in turn are mounted upon stacked linear axes. Each interface reduces the stiffness of the machine which is counter to the necessity for extremely high stiffness that is needed to support ductile regime grinding. High machine stiffness is essential to maintain consistent and high quality surface finish and to minimize wheel wear.

The new machine base is effectively two highly stiff ($>10\,000\text{ N}/\mu\text{m}$ radial and axial) rotary hydrostatic bearings. Conventional machines use linear axes to provide the primary motion between tool and component, the machine base has to resist machining forces in bending. The Twin Turret machine's turrets are bolted together via a solid base plate resisting the machining forces in tension rather than in bending.

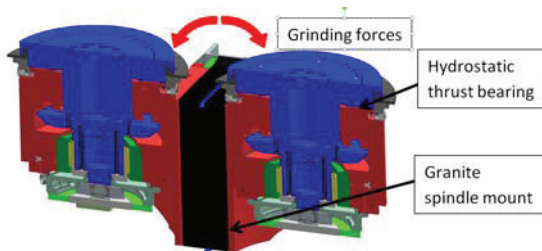


Figure 6: Rotary turret cross section.

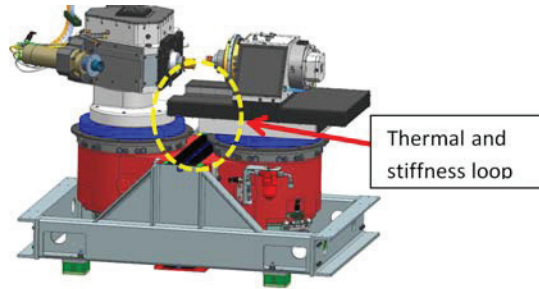


Figure 7: Thermal/stiffness loop of the Twin Turret design.

Using rotary axes as the primary machine axes enables complex geometrical traverse grinding as well as tool normal grinding without the need for an additional rotary axis. Multiple tools can be mounted upon a common and highly rigid turret. The turret shafts are precision ground and the very large diameter (440mm) bearing provides excellent averaging producing radial and axial spindle error motions of around 100nm

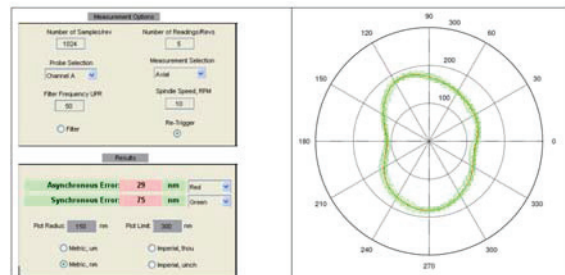


Figure 8: Axial turret error motion.

It is these rotary axes that provide the primary motion control for the machine, replacing conventional linear axes. They are very extremely stiff, axisymmetrically.

Peripheral wheels are mounted with the grinding wheel aligned to the turret axis, thus wheel imbalance and process forces are directly resisted by the bearing stiffness, not the axis servo. For cup wheel grinding the grinding wheel to component interface is maintained at tool normal, thus any imbalance force induced axis oscillation produces only a 2nd order motion between wheel and component.

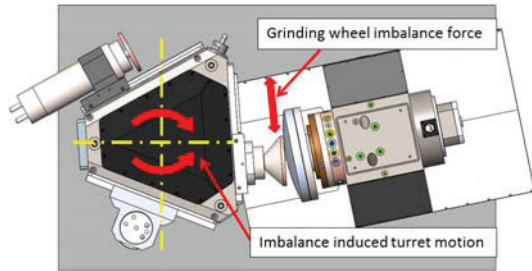


Figure 9: Wheel imbalance forces for cup wheel grinding.

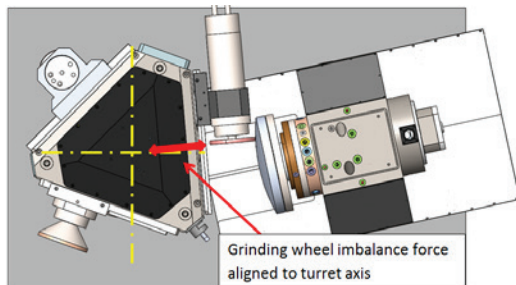


FIGURE 10: Wheel imbalance forces for peripheral wheel grinding.

GRINDING RESULTS

The grinding results presented here are for both cup and peripheral wheel grinding

Traverse ground aspheric surface

These results are for 'tool normal' grinding of an aspheric surface ground on a fused silica component. The traverse grind time using a metal bond wheel, Ø60mm, 6µm grit was 90 minutes. The form error of the ground surface is 280 nm P-V. This plot shows the result after a corrective run (using measurement data from Form Talysurf to provide the correction) and is thus effectively a measurement of the machine's repeatability, including thermal stability and process variability over 90 minutes.



FIGURE 11: Asphere grinding using cup wheel.

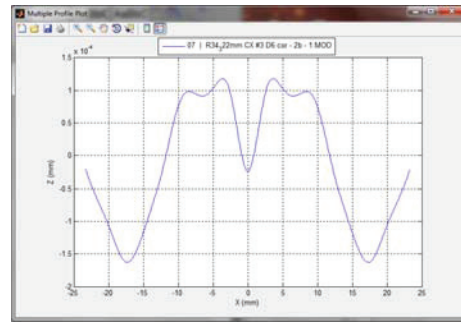


FIGURE 12: Ground asphere PV form error 280 nm PV (Form Talysurf, Zeeko Metrology Toolkit analysis).

Off Axis ground freeform surface

By interpolating the motion between the two main turret axes, the linear infeed axis and the rotary workhead axis, it is possible to maintain tool normal grinding of complex freeform surfaces. The grinding tool traverse paths can be spiral or raster scan. The test part was a double saddle, (concave and convex) inclined such that there is no line of symmetry on the surface.

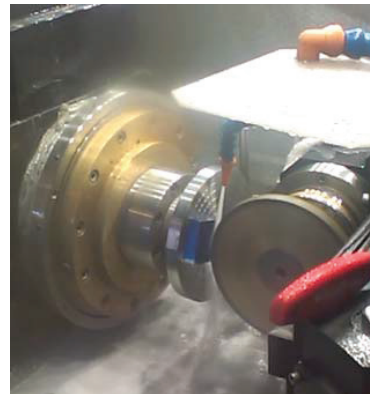


FIGURE 13: Ground off-axis freeform surface using a tool normal, interpolated raster scan motion path.

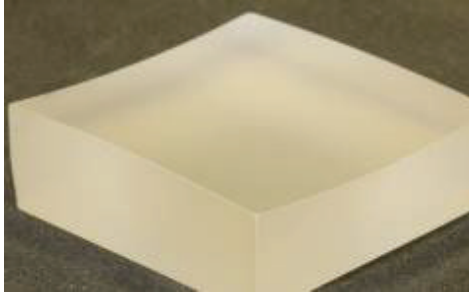


FIGURE 14: Rough ground surface (D42 metal bond wheel).

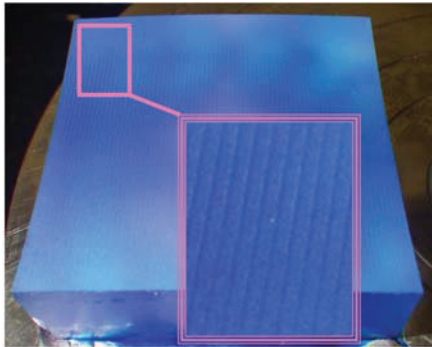


FIGURE 15: Rough ground surface shows raster scan path taken by grinding wheel.

Measurement data

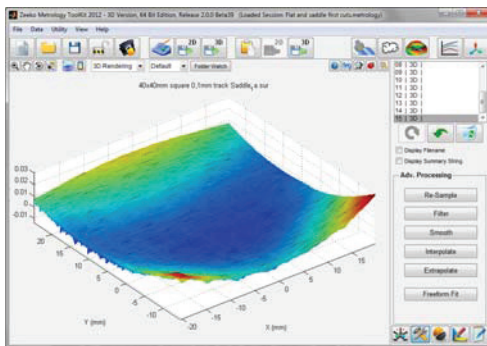


FIGURE 16: Form Talysurf measurement data, (Zeeko Metrology Toolkit analysis).

TABLE 1. Measurement data for free-form test pieces.

Part number	PV error μm	RMS error μm
1	44.8463	6.0892
2	45.7562	6.5044

These test piece measurements show the *uncorrected* ground freeform errors. They were ground without any correction for machine set up errors, wheel form errors, wheel wear etc. The objective of this test was to demonstrate the ability of the machine configuration to interpolate one linear and three rotary axes in order to produce a tool normal raster scan grinding path and to demonstrate the repeatability and thermal stability of the machine. The corrected asphere tests have demonstrated that with good measurement data it is possible to feed back the measured errors to produce sub-micron PV component form errors. Future work planned for the machine development will include 3D freeform error correction.

Conclusions

A novel machining platform has designed and developed compatible with a wide range of machining processes. Grinding results demonstrate that the design objectives of ultra-high stiffness and thermal stability have been achieved.

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TOOL WEAR CHARACTERISTICS AND THEIR EFFECTS ON FREEFORM SURFACE QUALITY IN ULTRA-PRECISION RASTER MILLING

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RESEARCH BACKGROUND

As the tool profile is directly imprinted on the machined surface during the freeform surface machining process, the wear of cutting tools directly affects the freeform surface quality. Research on tool wear characteristics and their effects on machined surface quality has mainly been conducted using conventional cutting [1], high speed milling [2], single point diamond turning [3], and micro milling process [4]. Very few such studies have used ultra-precision raster milling (UPRM) since it is a relatively complex cutting process [5-7]. Up to date, the most comprehensive research on the investigation of tool wear characteristics in UPRM was conducted by Yin, et.al (2009) [8], who concluded that tool wear characteristics were fractures or micro chipping due to impact effects in the UPRM process. However, his research mainly focused on the early stage of tool wear in UPRM and the effects of the tool wear on the machined surface quality, especially for freeform surface, were not considered.

The present research focuses on exploring tool wear characteristics and their effects on freeform surface quality in UPRM. Through a group of cutting experiments, it is found that tool wear characteristics in UPRM include cutting edge fractures, workpiece material welding, and wear land formation. Each tool wear characteristic has a different effect on the freeform surface quality. This research is meaningful since it provides a deep insight into the tool wear characteristics and their effects on the freeform surface quality in intermittent cutting process.

EXPERIMENTS

The experiments were conducted on the Precitech 705G CNC ultra-precision machine

(Precitech Inc. USA), the workpiece material was copper, cutting parameters are listed in TABLE 1. A desired sinusoidal surface was machined, after cutting a certain distance, the diamond tool was taken out to be inspected by a HITACHI TM3000 scanning electron microscope (SEM). Meanwhile, the workpiece was inspected by a WYKO NT 8000 microscope and an Olympus BX60 optical microscope.

TABLE 1. Cutting conditions in this experiment

Tool geometry parameters	
Tool type	APEX insert diamond tool
Rake angle	-2.5°
Clearance angle	15°
Tool radius	0.631mm
Swing distance	28.35mm
Cutting parameters	
Feed rate	200mm/min
Step distance	0.025mm
Spindle speed	4500 rpm
Depth of cut	0.03mm
Cutting strategy	Horizontal cutting
Cutting environment	Lubricant on

TOOL WEAR CHARACTERISTICS

FIGURE 1 shows tool wear characteristics at different tool wear stage. It indicates at the early tool wear stage (cutting distance about 1000 m), some fractures are found on the cutting edge that have no order and follow statistical law (see FIGURE 1 (a)). After cutting about 2000 m, some workpiece material was found welded to the rake face of the diamond tool (see FIGURE 1 (b)). It was also found that the small fractures disappeared in the cutting edge rounding process leaving only some relatively large

flattened fractures (F_1 and F_2 in FIGURE 1 (b)). These small fractures were thought to be

flattened and merged into the rounded cutting edge during the UPRM process.

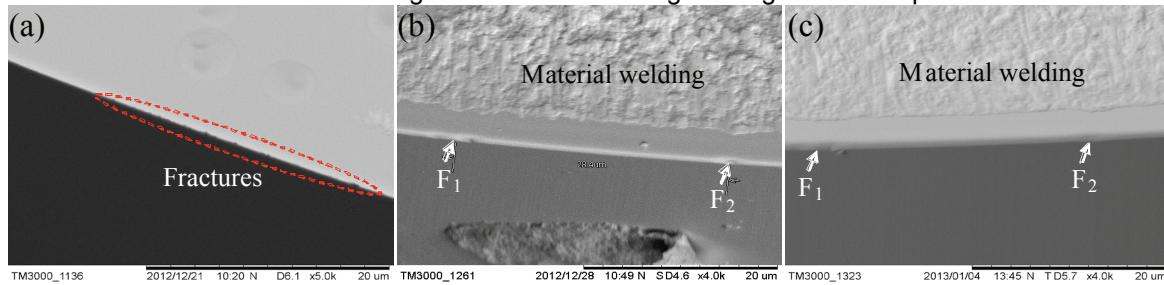


FIGURE 1. Tool wear characteristics at the cutting distance of a) 1000m, b) 2000m, c) 4000m

After cutting about 4000 m, it was found that the welded material still existed on the rake face of the diamond tool although it had become smoother due to the friction between the rake face of the diamond tool and the cutting chips. FIGURE 1 (c) shows that a wear land has formed with a width of about 1 μm , the relative large fractures that existed earlier stage (see F_1 , F_2 in FIGURE 1 (b)) have been flattened and are hard to distinguish at this cutting distance.

EFFECTS OF TOOL WEAR ON FREEFORM SURFACE QUALITY

During the UPRM process, the machined surface was generated by the repeat imprint of the cutting edges. Therefore, the occurrence of tool wear can directly affect the freeform surface finish. The appearance of fractures on the cutting edge can lead to the generation of 'ridges' on the freeform surface, as is shown in FIGURE 2. FIGURE 2 (a) indicates the 'ridges' imprinted by the fracture wear of diamond tool in a tool mark, while FIGURE 2 (b) shows the 'ridges' formed on the machined surface. The formation of ridges can deteriorate the freeform surface finish and affect the function of machined products.

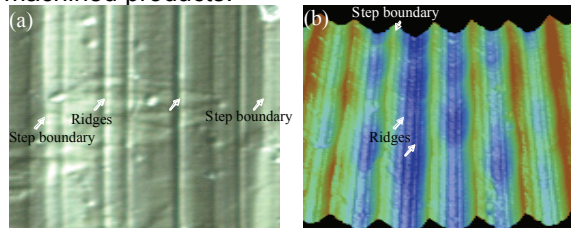


FIGURE 2. Ridges formed in a tool mark (Olympus BX60) and freeform surface (Wyko NT 8000)

The occurrence of material welding can lead to breakage of cutting chips, which can scratch the machined surface and lead to the formation of

burrs on the machined surface so that a poor surface finish is formed (see FIGURE 3). The breakage of cutting chips is caused by the increase of frictional force between the cutting chips and rake face of the diamond tool. This is because material welding can increase the frictional force.

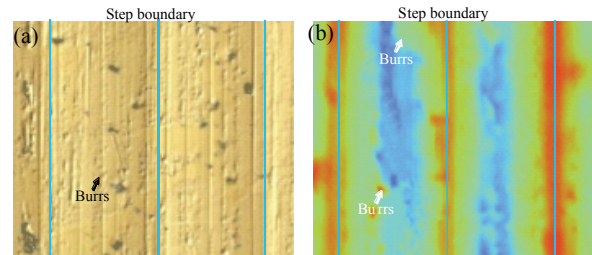


FIGURE 3. Burrs formed on the machined surface inspected by (a) Olympus BX60 microscope and (b) Wyko NT 8000 microscope

With the progress of diamond tool wear, a wear land is formed on the cutting edge, which owns 'crescent-like' morphology as shown in FIGURE 4 (a-c), that is wider in the middle while thinner at two sides of cutting edge.

The formation of wear land can make the original cutting edge losing and form a new cutting edge with a relative larger tool nose radius as illustrated in FIGURE 5 (a). In FIGURE 5 (a), point 'a' present the start of wear land and point 'b' denotes the end of wear land. The location of point 'a' and 'b' can be identified according to the surface structure has been machined. For example in this study, sinusoidal surface was machined, whose side curve is a Sine curve as shown in FIGURE 5 (b). During the UPRM process, the Sine curve is formed by the envelope of tool nose profile. Therefore, the maximum gradient of the Sine curve in FIGURE 5 (b) can be used to pinpoint the start point and end point of wear land as shown in FIGURE 5 (a), since the two points are the boundary points

of cutting edge for cutting and no cutting. The new formed cutting edge profile can be identified

under the given of wear land width and wear land angle.

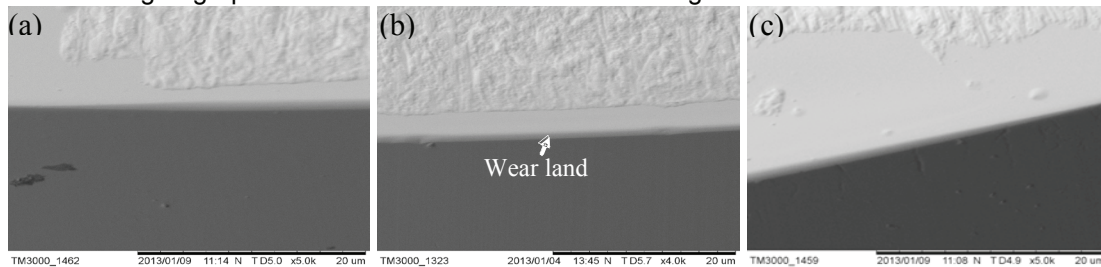


FIGURE 4. Tool flank wear topography at the left, middle and right sides of cutting edge

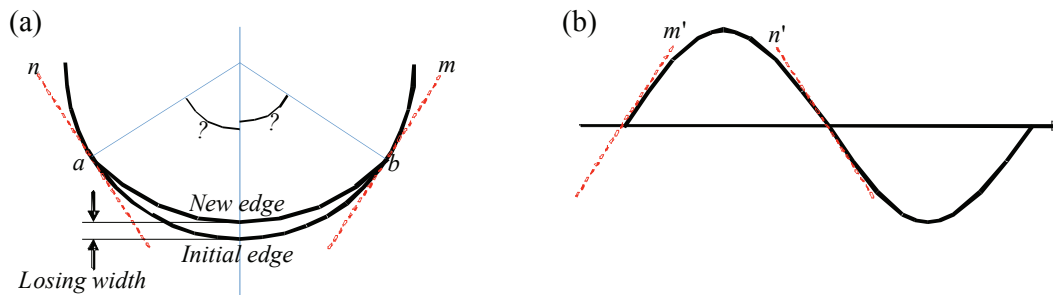


FIGURE 5. Freeform surface and the related tool wear topography

The occurrence of tool flank wear can affect the form accuracy of machined freeform surface: wear land formation can increase the tool nose radius and reduce the swing distance. Cutting a freeform surface with a worn tool and original cutting parameters will lead to the form error. As

is shown in FIGURE 6, the theoretical sinusoidal surface and that machined by a worn tool have a deviation, the deviation achieve the maximum value as the gradient equal to 0° , whose value is the losing width of cutting edge.

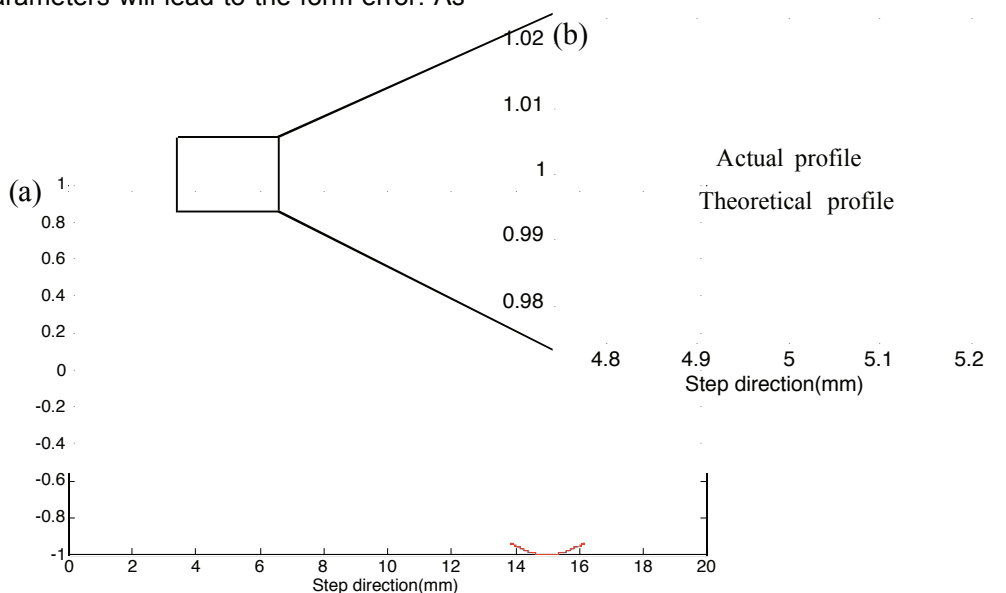


FIGURE 6. Comparison of the theoretical profile and actual profile of the sinusoidal surface in side view

The deviation curve between theoretical sinusoidal surface and actual one in side view is

shown in FIGURE 7. It is found that the error curve caused by tool flank wear can be

approximated into the absolute value of Sine function with its amplitude equal to the losing width of cutting edge, period equal to the freeform surface length. Therefore, the form error can be formulated and compensated. Normally, the form error of freeform surface caused by tool flank wear in UPRM can be compensated by two methods: changing cutting parameters (tool nose radius and swing distance) and re-cutting according to the error curve. Both methods can achieve form error compensation.

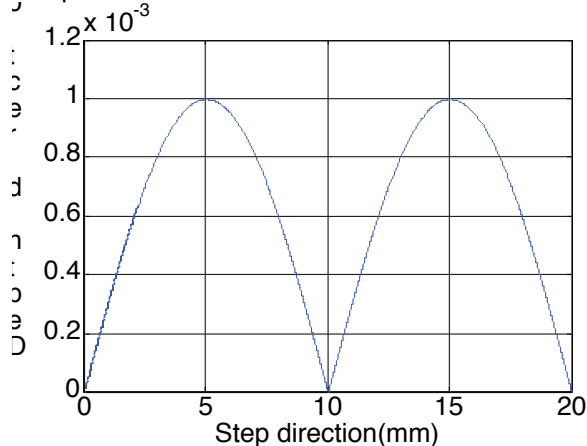


FIGURE 7. Error curve of theoretical profile and actual profile with respect to the distance in step direction

Since the flank wear land identification is based on the surface structure being machined, the compensation method of flank wear effects on the form error of freeform surface can also be used in other freeform surfaces.

CONCLUSIONS

This study investigated tool wear characteristics in UPRM and their effects on freeform surface quality. Specific conclusions drawn from the study are:

- (1) At the early tool wear stage, tool wear characteristics tend to be fractures of the cutting edge that are imprinted on the freeform surface and form a group of ridges.
- (2) Material welding is a tool wear form in UPRM that can make the cutting chips broken, which scratch the freeform surface and lead to the formation of burrs.
- (3) At the steady tool wear stage a wear land is formed, whose profile is 'crescent-like'. The occurrence of wear land can affect the form accuracy of machined freeform surface.

However, the form error can be compensated by changing cutting parameters.

ACKNOWLEDGEMENTS

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DEVELOPMENT OF MICRO/NANO-SCALE OPTICAL PROFILOMETRIC TECHNIQUES FOR FREEFORM AND OFF-AXIS AXISYMMETRIC SURFACES

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ABSTRACT

This study presents a broadband differential confocal surface profilometer using double-slit chromatic confocal measurement for freeform and off-axis axisymmetric surface inspection. A multi-wavelength differential confocal surface profilometer was developed by employment of double-slit conjugate configuration for generating the differential gradient in confocal measurement. The differential gradient is generated by correlating two focus-depth-response curves which are measured by the two imaging units with their corresponding slits. The developed system can achieve one shot inspection for line-scan profilometry without vertical scanning required by conventional confocal measurement, thus the scanning efficiency is enhanced. A long vertical measurement range can be achieved for a range of a few hundreds of micrometers while its vertical resolution is capable of reaching a level of less than 0.5 micrometers.

Keywords: Confocal measurement, differential confocal microscopy, chromatic confocal measurement, surface profilometry.

INTRODUCTION

Chromatic confocal technique with an exemption for vertical scanning can achieve real-time profile measurement [1,2] and reach a long depth measurement range [3]. Differential laser confocal principle [4-7] is capable of real-time measurements and doesn't need vertical scanning, but its depth measurement range is relatively small and still needs scanning for both X and Y axes, thus reducing its application for *in-situ* profilometry. To speed on the measurement of chromatic confocal system, the concept adopted in this research is to synthesize the chromatic confocal microscopy with the differential technique for significantly strengthening the measurement efficiency. A chromatic differential confocal microscopy has been developed by the authors with two color CCDs to be placed in front and behind the focal

plane to obtain differential confocal signals for achieving one-shot measurement without vertical scanning [8].

However, in the above method, there still exist some drawbacks which may impact the accuracy and its potential application. Since the system is based on two color CCDs placing in front and behind the focal plane to achieve differential configuration, respectively, the images obtained by these two CCDs may be with two different fields of view (FOV) when a line-scanning detection is employed. In addition, the defocusing of the detected two images can also impact the signal quality for accurate line-scan detection. Therefore, while the range of lateral scanning range can be reduced and the measurement accuracy can be degraded, the applicability of the above approach to *in-situ* surface measurement may be not realistic.

Therefore, to avoid the abovementioned problems, this research proposes one novel optical configuration of chromatic differential confocal microscopy (CDCM). The configuration uses two different sizes of light emitted by two slits with different slit opening for generating different full width at half maximum (FWHM) of the intensity response curve. The approach then differentiates two signals captured from two respective imaging sensors by using signal normalization to obtain a differential signal for measured height conversion. With this, the design can prevail over the other existing confocal methods because it effectively resolves the above mentioned problems by a simple but robust optical configuration. Moreover, the two image sensors can match the same measuring field of view, thus allowing achieving easy system alignment and maximizing its detection range.

OPTICAL SYSTEM DESIGN AND MEASURING ALGORITHM

The proposed optical system [9] first divides the incident light source into two signals by an

optical fiber, shown in Fig. 1. Importantly, two different slits with two opening sizes are accurately placed at exits of fibers for spatial filtering to make the incident lights to be precisely formed linear inspecting light. With a polarization beam splitter, two sets of line lights are divided into P-wave and S-wave, to avoid two sets of light to be mixed together. Then, two line lights go through an axial chromatic dispersion objective, in which the two polarized lights are chromatically dispersed along the optical axis and illuminated on the tested object surface. The reflected lights then go through the chromatic objective again, then the beam splitter and the polarization beam splitter for making the reflected optical signal separated, then two individual slits with different opening for generating two filtered light, and then finally received by two individual CCDs, respectively. These two acquired slit images are further processed by a differential normalization operation described in the following session.

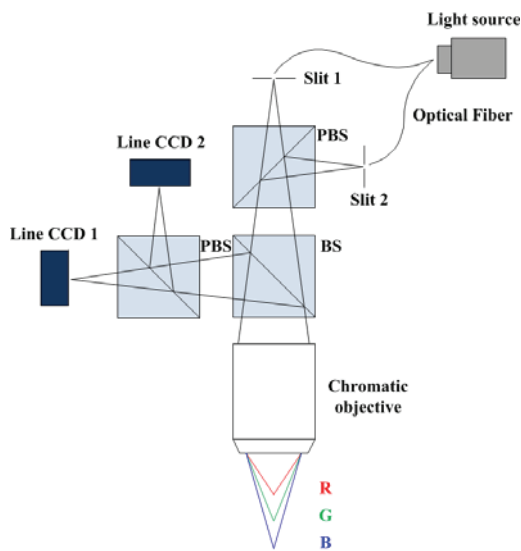


Figure 1. Schematic diagram of the chromatic differential confocal configuration. (USA and Taiwan Patents)

The light intensity function of the two acquired signals can be described [8,9] as follows:

$$I_{\lambda}(v, w, u) = \left\{ S_{\lambda}(v, w) \otimes |h_{\lambda}(v, w, u)|^2 \right\} \quad (1)$$

$$\left\{ D_{\lambda}(v, w) \otimes |h_{\lambda}(v, w, u)|^2 \right\}$$

where λ is the responding wavelength range in the CCD; the source ($S_{\lambda}(w)$) and detector ($D_{\lambda}(w)$) distributions are expressed in coordinates corresponding to the object plane; h is the amplitude point spread functions for lens; $\otimes$ denotes convolution; and, v, w, u are the optical coordinates.

The intensity distribution in Eq. (1) does not consider the reflectance variance of each measured point of the tested surface. The two signals received by the two perspective imaging sensors with different slit sizes can be generally modeled in Eqs. 2 and 3. The opening width w of the slits being employed here can be adequately set at a range of from a few tens to several hundreds of micrometers. As shown in Fig. 2, the two signals detected from the same detected sample point but with a different slit opening, in which one is 200 μm and another is 20 μm , are peaked at the same focus position but with a different focus-depth-response curve, where the one with the wider opening slit is distributed with a higher FWHM than the one with a narrow opening slit. Here, n_g represents the reflectance of the tested sample point.

$$I_1(w, u) = \left\{ S_1(w) \otimes |h_1(w, u)|^2 \right\} \left\{ D_1(w) \otimes |h_1(w, u)|^2 \right\} \quad (2)$$

$$I_2(w, u) = \left\{ S_2(w) \otimes |h_2(w, u)|^2 \right\} \left\{ D_2(w) \otimes |h_2(w, u)|^2 \right\} \quad (3)$$

To establish a reflectivity-independent differential ratio, signal normalization on the two signals modeled in Eq. (2) and (3) is employed to remove the potential variance caused by potential random variation of the object's surface reflectivity. As defined in Eq. (4), a differential confocal ratio, $RC(w, u)$, is established to normalize the surface reflectance variance. In Fig. 3, this algorithm is used to normalize the reflectance n_r of the tested sample, making the differential confocal ratio invariant to n_r and a linear relationship between the detected object depth and the ratio to be established precisely within a measurable depth range.

$$RC(w, u) = \frac{I_1(w, u + u_m) - I_2(w, u - u_m)}{I_1(w, u + u_m) + I_2(w, u - u_m)}$$

$$= \frac{n_r(w)I_1(w, u + u_m) - n_r(w)I_2(w, u - u_m)}{n_r(w)I_1(w, u + u_m) + n_r(w)I_2(w, u - u_m)} \quad (4)$$

$$= \frac{I_1(w, u + u_m) - I_2(w, u - u_m)}{I_1(w, u + u_m) + I_2(w, u - u_m)}$$

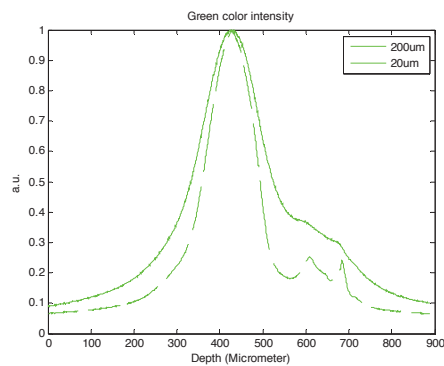


Figure 2. Focus-depth-response curves of two imaging sensors obtained by vertical scanning.

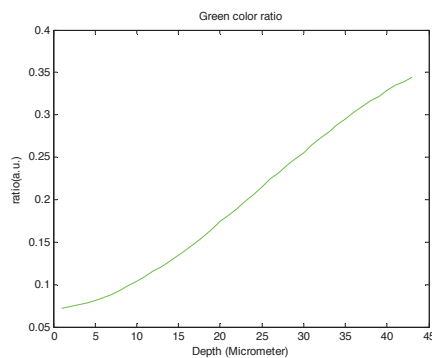


Figure 3. Relationship between differential confocal ratio and object's depth.

EXPERIMENT RESULTS AND ANALYSES

To attest the measurement accuracy of the developed measurement approach, an experimental measurement was conducted on a pre-calibrated step-height target with a pre-calibrated step height with a nominal size of $25.400\text{ }\mu\text{m}$. Using the proposed measuring method, the measurement result of the target was obtained and the top view of reconstruction as well as the cross-section contour of the step height are shown in Figure 4(a) and (b), respectively. The step-height measurement result in a 30-time repeatability test to confirm the repeatability was performed for a standard deviation of 60 nm being confirmed. This indicates that the developed double-slit chromatic differential confocal microscopy (CDCM) is proved realistic and effective for sub-micrometer precision in one-shot line-scanning profilometric microscopy.

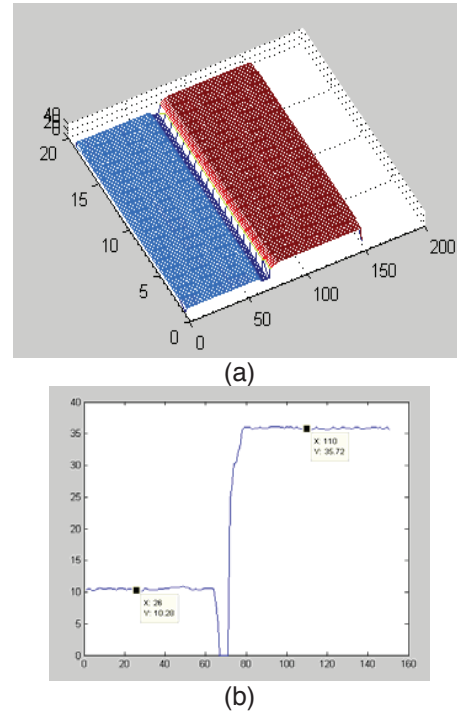


Figure 4. Results of repeatability measurement using pre-calibrated step-height target: (a) top view of the 3-D reconstructed image; and (b) cross-section profile.

Meanwhile, a plastic lens with freeform surface was measured for its cord thickness along the optical principle axis. The plastic lens shown in Figure 5 was fabricated by a precision plastic molding. Its cord thickness had to be measured and checked with its CAD design, thus its assembly position in the housing is ensured.

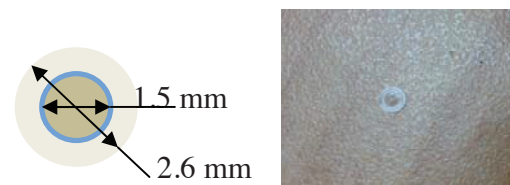


Figure 5 the tested plastic lens with its global dimension.

By using the developed measurement method, the physical sample, the detected signal corresponding to the central lens thickness can be shown in Fig. 6, respectively. From the measured results, it indicates that the developed method is capable of measuring industrial micro structures, in which the tested surface has a double side of freeform surfaces. The measured thickness was $154.76\text{ }\mu\text{m}$ with one standard

deviation of 0.3 μm . The 3-D freeform surface of the tested lens was measured and illustrated in Figure 6, in which a cross-section diagram indicates the surface has a certain degree of scratch on the top surface.

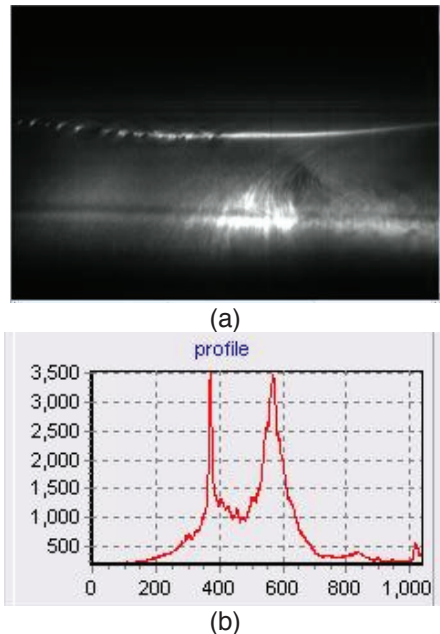


Figure 5. Measurement results of the tested plastic lens used in 3C product: (a) the detected confocal image and (b) the detected focus-depth-response curve along the optical principle axis of the lens.

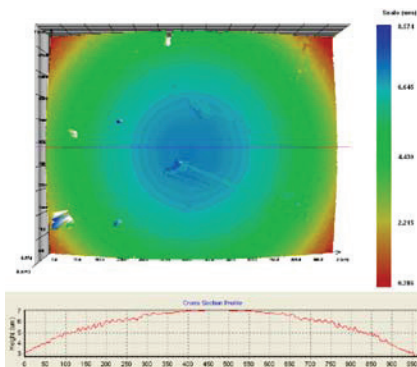


Figure 6. The freeform 3-D measurement of the tested lens surface.

CONCLUSIONS

This study presents broadband differential confocal surface profilometry using a new double-slit optical configuration for achieving in-situ one-shot microscopic surface profilometry. The proposed method is unique in its differential optical structure for obtaining one-shot

microscopic surface measurement. The experimental tests indicate its proved applicability to industrial automated optical inspection in achieving a sub-micrometer measuring resolution and a depth measuring range from a few tens to hundreds of micrometers for freeform optical lenses. Potentially, the measuring speed of the developed system could be tuned up to the maximum speed of the line optical sensor, usually up to 50k lines per second.

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NANOMETER PROFILE MEASUREMENT OF ASPHERIC SURFACE USING SCANNING DEFLECTOMETRY AND ROTATING AUTOCOLLIMATOR: SELF-CALIBRATION METHOD OF AUTOCOLLIMATOR

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INTRODUCTION

High-accuracy aspherical mirrors and lenses with large dimensions are widely used in large telescopes and other industry fields. However, the measurement methods for large aspherical optical surfaces are not well established. Scanning deflectometry is used for measuring optical signals near flat surfaces with uncertainties on subnanometer scales [1-3]. A critical issue regarding scanning deflectometry is that high-accuracy autocollimators (AC) have narrow angular measuring ranges and are not suitable for measuring surfaces with large slopes and angular changes [4-6]. The goal of our study is to measure the profile of large aspherical optical surfaces with an accuracy of approximately 10 nm.

We have proposed a new method to measure optical surfaces with large aspherical dimensions and large angular changes by using a scanning deflectometry method. A rotating AC was used to increase the allowable measuring range. Error analysis showed that a rotating AC reduces the accuracy of the measurements.

In this study, we developed a new AC with CMOS as a light-receiving element. The CMOS-type AC can measure wider ranges of angular changes, with a maximum range of 21500 μrad (4300 arcsec) and a stability of 0.1 μrad (0.02 arcsec). We conducted an experiment to measure an aspherical optical surface (non-axis parabolic mirror) with an angular change of 0.07 rad (4 arcdeg). The repeatability for ten measurements of a non-axis parabolic mirror was less than 4 nm.

In the systematic error, the nonlinear error of the AC is large factor. Therefore, we propose a new method of self-calibration of nonlinear error of the AC. Using the self-calibration method, the reproducibility by reference conditions of the AC improved.

MEASUREMENT PRINCIPLES

Rotating AC

The basic measurement principle is shown in Fig. 1 [7]. A rotary stage is fixed on a linear stage. On the rotary stage, an AC is fixed, and by translation of the linear stage, the surface is measured. When measuring surfaces with large slopes, the rotary stage is used to help increase the measuring range. When the angle measured with the AC is going to exceed the maximum range, the rotary stage turns a certain angle. Consequently, the measured angle returns into the allowable measuring range. Then, by linear translation, scanning of the whole surface continues. Integrating the angle data gives the profile of the optical surface

Figure 2 (a) shows the angular data measured from the AC [7]. Because the AC rotates, the measured angle is not continuous. The connected angle data is calculated from the measured angle and the profile data in Fig. 2 (b) was by integrating of the connected angle data.

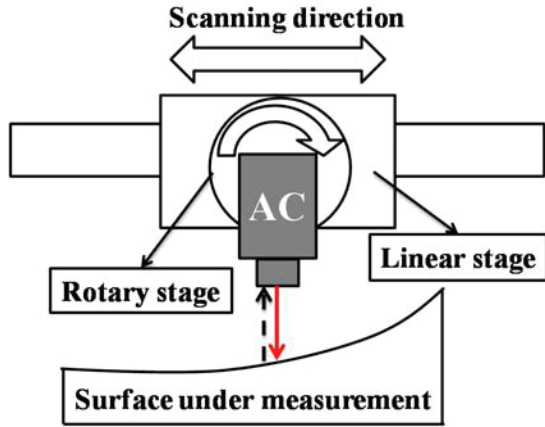
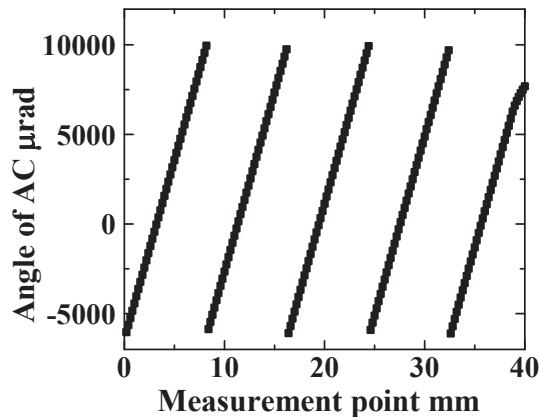
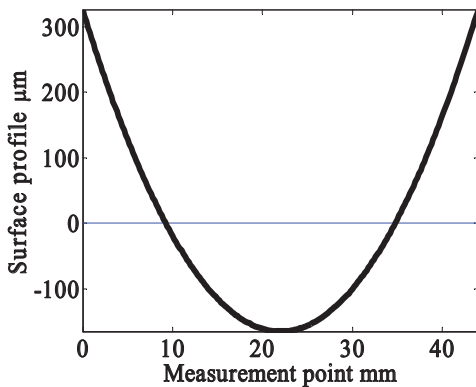


FIGURE 1. Principle of rotary AC method for increasing measuring range of deflectometry method [7].



(a) Angle data measured by AC.



(b) Profile data with subtracted least square line.

FIGURE 2. Profile measurement by rotating AC [7].

Connected Angle

The connection method used for the angle data is a key procedure of the proposed measurement method. Because the AC rotates, a ray of light turns a certain angle α , so that the measured point also moves by S , as shown in Fig. 3. Consequently, the angle measured by the AC is interrupted by rotation. To connect the angular data, the distance moved S , caused by rotation, and the angular change A , caused by the change in distance must be known.

The repeatability of a motorized rotary stage with a wide range is usually large. Therefore, we have proposed a method to calculate the rotated angle α , of the rotary stage from the AC data [8]. Because θ is an angle on the order of several tens of thousands of μrad , we can assume the circular arc length $R\theta$, is the same as the circular arc length, αD . Thus, the relationship between α and β is given by Eq. (1).

$$\alpha = \theta + \beta, R\theta = \alpha D, \alpha = \frac{R}{R-d}\beta \quad (1)$$

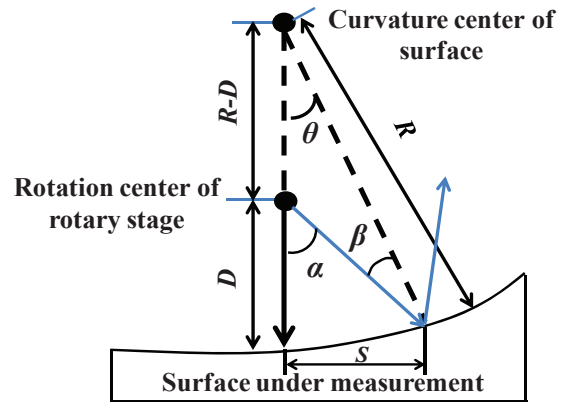


FIGURE 3. Relationships between angles α and β when AC rotation angle is θ [8].

WIDE RANGE AC

CMOS-Type AC

We have developed a new wide range AC with CMOS as an optical image element (CMOS-type AC) [9]. Figure 4 shows the photograph of the CMOS-type AC and Table 1 lists specifications and performances of the CMOS-type AC. The design is simple and low cost elements are selected. In addition, the CMOS image sensor is directly connected to the personal computer by USB interface. Therefore, data processing system is simple and low cost.

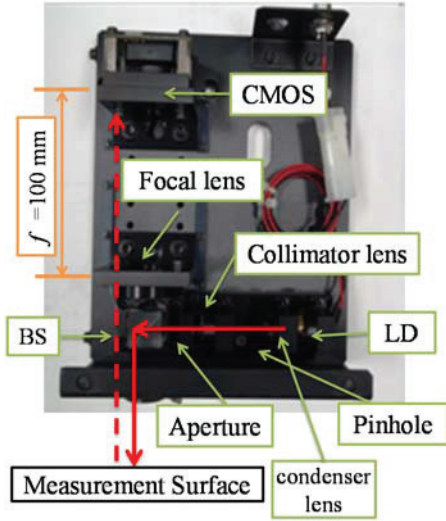


FIGURE 4. Wide range AC with CMOS image element (CMOS-type AC) [9].

Table 1. Specifications of the CMOS-type AC [9]

Specifications	Value
Light source	LD (634 nm)
Diameter of beam	1.7 mm
Image element	2560 (H) × 1920 (V)
Focal length	100 mm
Measurement range	21500 μ rad (4300 arcsec)
Stability	0.1 μ rad (0.02 arcsec)
Pixel resolution	11.0 μ rad
Calculated resolution	0.05 μ rad

Self-Calibration of AC

The Developed CMOS-type AC has large measuring range and high stability. However, the linearity of the CMOS-type AC is not good. The linearity may vary by the environmental conditions, the working distance, and the curvature of the measured workpiece. Therefore, we developed the on-machine self-calibration method for CMOS-type AC.

Figure 5 illustrates the basic concept of the proposed self-calibration method. First, the measured workpiece is scanned; then, the measured workpiece is scanned again after the workpiece is tilted of angle t . The linearity function of the AC f is calculated by equation (2). Where, $f(p_1)$ is scanning angle data before tilt and $f(p_2)$ is that after tilt.

Figure 6 shows an example of self-calibration. The measured workpiece is spherical mirror of

curvature radius of 5000 mm, and tilt angle is about 1000 μ rad (200 arcsec). Figure 6 (a) shows angular difference between before and after tilt. Then, the integration of the angular difference, the linearity curve f is calculated in Figure 6 (b).

$$\begin{aligned}
 f(p_1) &= \frac{x}{R} \\
 f(p_2) &= \frac{x}{R \cos t} - t = \frac{x}{R} - t \\
 f'(p_1) &= \frac{f(p_1) - f(p_2)}{t} \\
 f(p_1) &= \sum f' = \sum \frac{f(p_1) - f(p_2)}{t}
 \end{aligned} \quad (2)$$

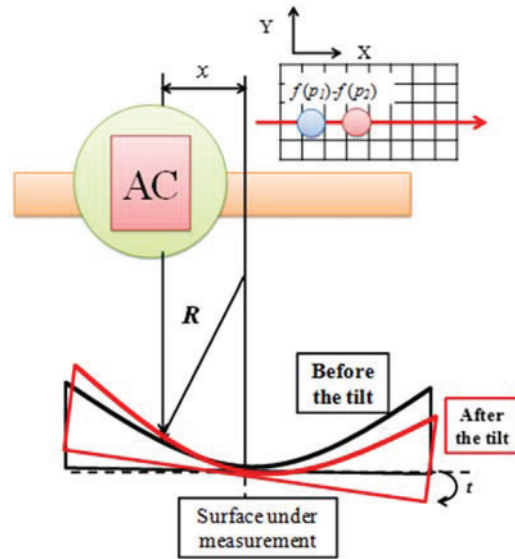
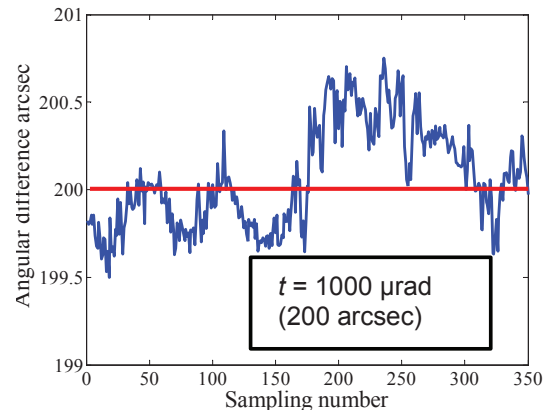
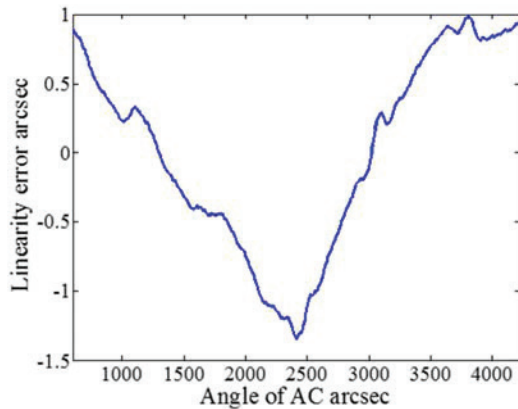


FIGURE 5. Principle of self-calibration: the linearity curve f of AC is calculated by angular difference before and after tilt.



(a) Angular difference before and after tilt.



(b) Linearity curve of AC by self-calibration.

FIGURE 6. Example of self-calibration of AC.

EXPERIMENT

Experiment Setup

An experimental setup was constructed, as shown in Fig. 9 [9]. The surface under measurement is fixed vertically in the holder. The linear stage is parallel to the base of the mirror. A motorized rotary stage with the CMOS-type AC is fixed on the linear stage. The laser coming from the CMOS-type AC is reflected by the surface under measurement, and the laser subsequently returns to the CMOS-type AC.

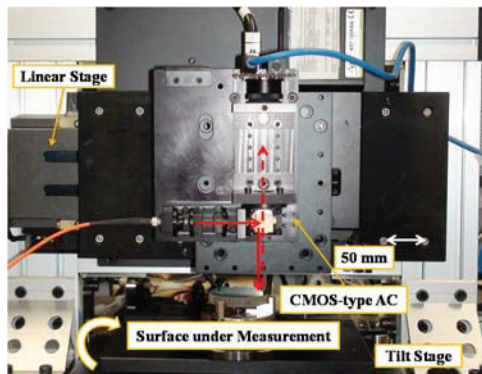


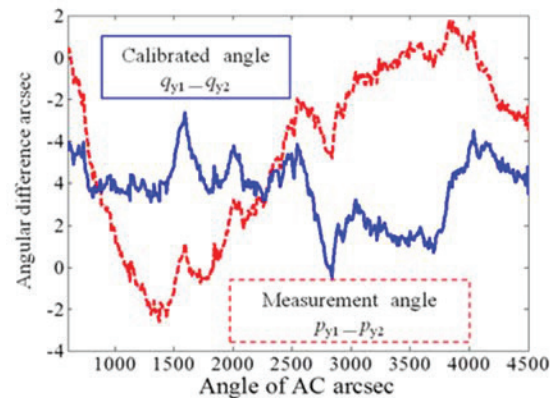
FIGURE 7. Experimental setup of CMOS-type AC, linear stage, tilt stage and measured workpiece [9].

Effect of Self-Calibration of AC

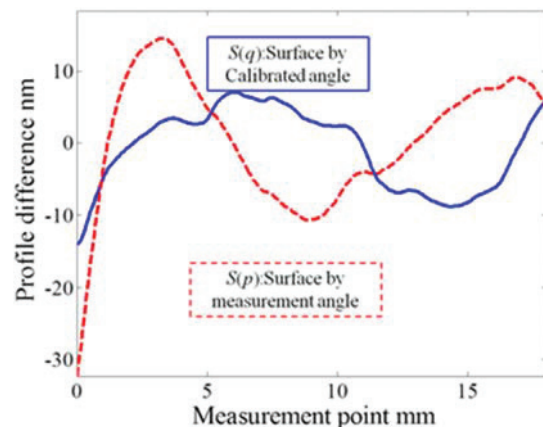
The effect of self-calibration is evaluated by the comparison between two measurements on the different positions on CMOS. The position of CMOS affects the linearity of AC because the distortion of lens system may different on each position of CMOS.

Figure 8 shows the comparison between two measurements. Figure 8 (a) displays the angular difference of two measurements. The angular difference with self-calibration is smaller than that without self-calibration. Figure 8 (b) displays the profile difference of two measurements. The profile difference value with self-calibration is also smaller than that without self-calibration. From the basic experiments, we confirmed the effect of self-calibration for our proposed method.

An off-axis parabolic mirror with a diameter of 50.8 mm was measured [9]. The profile of the mirror is illustrated in Figure 9. The angular change was approximately 0.07 rad, the scanning length L was 40 mm; the scanning step h was 0.2 mm. The repeatability (std) of the profile was less than 4.0 nm.



(a) Comparison of measured angles on two positions of CMOS with/without self-calibration.



(b) Comparison of measured profile on two positions of CMOS with/without self-calibration.

FIGURE 8. Evaluation on effect of self-calibration of AC.

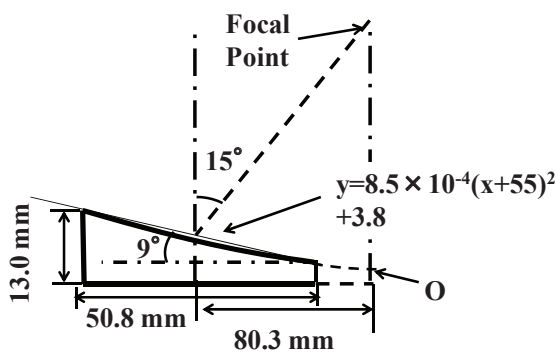


FIGURE 9. Profile of measurement object (off-axis parabolic mirror) [9].

CONCLUSION

We proposed a new method of measuring large aspherical optical surfaces using a rotating AC; the method is based on a scanning deflectometry method. To reduce the error of the profile, we developed a CMOS-type AC system with a wide measuring range. The measuring range of the developed CMOS-type AC is 21500 μ rad. For CMOS-type AC, we developed the self-calibration method using tilt of the measured workpiece. The effect of self-calibration of AC is evaluated by the basic experiments. Furthermore, we measured the surface profile of an off-axis parabolic mirror having an angular change of 0.07 rad. The repeatability (std) was less than 4.0 nm.

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SYSTEMATIC ERROR ANALYSIS OF NANOPROFILER USING NORMAL VECTOR TRACING METHOD BASED ON NUMERICAL SIMULATION

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INTRODUCTION

High-accuracy aspheric or free-form optical elements are demanded nowadays. To develop practical application using highly coherent X-ray light source (for instance, X-ray free-electron laser sources such as the SPring-8 Angstrom Compact Free-Electron), nanofocusing of laser is indispensable, and high-accuracy aspheric mirrors are needed for that matter. Next-generation lithography applications using extreme ultraviolet or soft (13.5 nm wavelength) X-ray light sources also need high-accuracy aspheric mirrors for projection optical systems. Free-form optical elements with radii of curvature of less than 10 mm are now employed for consumer products represented by digital cameras [1–2]. Measurement is essential for the creation of such high-accuracy optical elements. Most often measuring methods such as interferometers and coordinate measuring machines (CMMs) do not have enough accuracy for these demands [3–4]. Interferometers depend on the precision of a reference surface, and CMMs have limits of valid measurement angles. The novel methods which aim to measure aspheric or free-form surface such as a stitching interferometry [5] and a deflectometry [6] do not have enough accuracy. Aiming to meet the demands, we developed novel nanoprofiler that traces the normal vector of a mirror's surface. The normal vector is measured using the straightness of laser light. The optical motion system which determine the direction of laser propagation, have two pairs of goniometers. And the sample motion system has two pairs of goniometers and one linear stage. By synchronous drive of the stages, the incident light and reflected light to the mirror surface is made to be coincident, and then normal vector is obtained. Our measuring method uses the information of the normal

vector and coordinates of each measuring point. This information is post-processed by a reconstructed algorithm, and the three-dimensional surface figure is obtained. The profiler does not depend on reference surface accuracy, and free-form surfaces can be measured by proposed method. Proposed method using nanoprofiler have possibility to realize the absolute surface figure measurement of next-generation high-accuracy mirror. It is confirmed that developed nanoprofiler have the repeatability of sub-nanometer scale by experimental evaluation. In order to guarantee accuracy of form absolutely, it is indispensable to get to know the uncertainty of a form measurement result. Then, the influences which it has to a measurement result by errors are investigated using numerical simulation. In order to solve the influence of a systematic error, the simulation was carried out paying attention to the stage assembly errors of nanoprofiler. There are assembly errors of 6 degrees of freedom (3-axis rotation and 3-axis translation) in the assembly of the stage. We investigated the effect of each assembly errors which give the surface figure measurement. In addition, the sensitivity of the assembly errors is obtained by the partial differential respectively. As a result, it was found that the influence of the assembly errors is linear to the figure error. Under the influence of an assembly error, which shows the distribution constant was found to figure errors. And the influence of the error in the y-axis connecting the sample system and the optical system is the most dominant was found.

METHOD OF MEASUREMENT

Figure 1 illustrates the principle underlying the proposed profiler, which is based on the

straightness of laser light and the high accuracy of rotational goniometers [7–8]. Our measurement method involves two components: an optical head and a sample motion unit. The optical head motion unit consists of two goniometers (rotating around the θ - and ϕ - axes, respectively), one linear motion stage (moving along the y-axis), and a sample motion unit consisting of two goniometers (rotating around the α - and β - axes, respectively). Using this setup, the normal vectors (n_x , n_z) and coordinates (x , z) of measurement points on a sample can be determined and, through the application of an original algorithm, used to reconstruct a surface shape. A laser light source and quadrant photodiode (QPD) detector are installed at optically equivalent positions at the rotation centers of the goniometers of the optical head motion unit; by causing the optical paths of incident and reflected light to coincide, the objective coordinates and normal vectors at a measurement point can be obtained. To achieve this, the motions of the four goniometers are used to control the reflected light so that it continually returns to the center of the QPD. During the measurement process, the optical path length (L) is kept constant by applying a numerically controlled shift (Δy) to the position of the linear motion stage.

Based on the coordinates of the goniometers and the linear stage, the coordinates of each normal vector can be determined. Equations 1 and 2 show the coordinates of the measurement point (x , y , z), and the normal vector (n_x , n_y , n_z), respectively. In order to produce reconstructed shapes with higher precision, a reconstruction algorithm is used to determine shapes in terms of normal vectors (n_x , n_z) and their coordinates (x , z), as this method is more accurate than using y and n_y directly.

$$\begin{pmatrix} x \\ y \\ z \end{pmatrix} = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos \beta & \sin \beta \\ 0 & -\sin \beta & \cos \beta \end{bmatrix} \begin{bmatrix} \cos \alpha & \sin \alpha & 0 \\ -\sin \alpha & \cos \alpha & 0 \\ 0 & 0 & 1 \end{bmatrix} \begin{pmatrix} -L \cos \phi \sin \theta \\ L \cos \phi \cos \theta + \Delta y - (L - R_y) \\ L \sin \phi \end{pmatrix} \quad (1)$$

$$\begin{pmatrix} n_x \\ n_y \\ n_z \end{pmatrix} = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos \beta & \sin \beta \\ 0 & -\sin \beta & \cos \beta \end{bmatrix} \begin{bmatrix} \cos \alpha & \sin \alpha & 0 \\ -\sin \alpha & \cos \alpha & 0 \\ 0 & 0 & 1 \end{bmatrix} \begin{pmatrix} -\cos \phi \sin \theta \\ \cos \phi \cos \theta \\ \sin \phi \end{pmatrix} \quad (2)$$

The profiler then calculates surface shapes from the normal vectors and coordinates determined by the algorithm by measuring the rotary motion of the goniometers, which is more accurate than measuring linear motion and requires no reference mirrors. Therefore, there are no limitations on

which shapes can be measured and free forms can be directly mapped [9].

We developed an algorithm to calculate three-dimensional surface profiles based on the fact that a normal vector of light is equivalent to the surface slope profile when the incident and reflected light paths are coincident. In this algorithm, the surface profile is expressed as a Fourier series whose derivative is least-squares fitted to the acquired data.

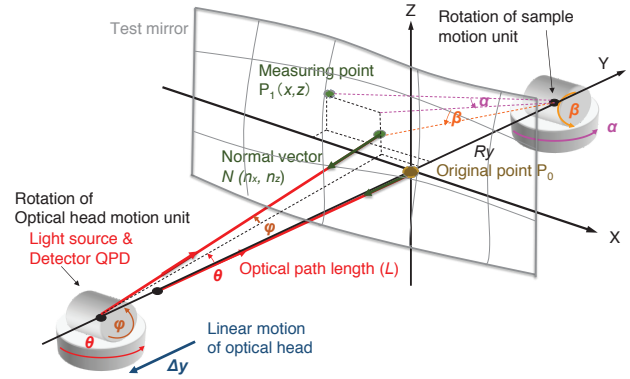


FIGURE 1. Principle behind our system for profile measurement in two dimensions.[10]

NANOPROFILER

Figures 2 and 3 show a schematic view and photograph of the nanoprofiler. The machine operates on five motion axes, using one linear motion stage in the optical head motion unit (YF-axis), two sets of twin goniometers in the optical head motion unit (CF-, AF-axes for θ -, ϕ -directions), and a sample motion unit (CW-, AW-axes for α , β -axes). The resolutions of the linear motion stage and the goniometers are 1 nm and 0.035 μ rad, respectively. With the five axes controlled numerically, a laser beam scans the surface of the sample and the QPD detects the reflected laser point; the distance of this point from the center of the QPD is used in the computation of the normal vectors. The incident and reflected light beams and translation stage are all kept mutually parallel. In order to maintain the diameter of the laser spot on the sample at constant value, the beam from the semiconductor laser is condensed using two focusing lenses. The length of the optical path between the QPD and the sample surface is constant.

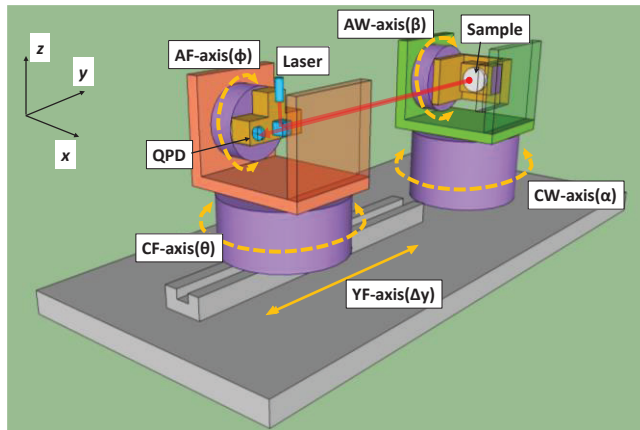


FIGURE 2. Schematic view of nanoprofiler.

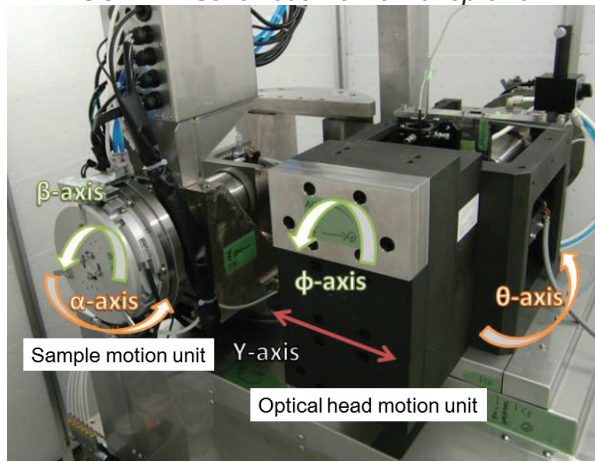


FIGURE 3. Photograph of nanoprofiler. [10]

SYSTEMATIC ERROR ANALYSIS BASED ON NUMERICAL SIMULATION

We carried out the analysis of systematic errors. Giving a systematic error of 6 degrees of freedom to the stage assembly of six of the device and shape is derived. As shown in FIG 4, YF is a translation stage in the y-axis direction is placed in the optical head motion system, c-axis rotary stage CF is placed on the YF, a -axis rotary stage AF is placed on the CF. On the other hand, sample motion system is assembled in the order c-axis rotation stage CW, Y-axis translation stage YW, in the a-axis rotation stage AW from the bottom. YW is adjustable stage, are not used for actual measurements. Measurements were performed simulation of concave spherical surface of $R = 100$ mm, respectively YF-on-G, CF-on-YF, AF-on-CF, CW-on-G, YW-on-CW, the AW-on-YW assembly error of 6 degrees of freedom is given to. Error of $-3\mu\text{rad}$, $-2\mu\text{rad}$, $-1\mu\text{rad}$, $1\mu\text{rad}$, $2\mu\text{rad}$, $3\mu\text{rad}$ is given to the a, b, c-axis. And error of $-3\mu\text{m}$, $-2\mu\text{m}$, $-1\mu\text{m}$, $1\mu\text{m}$, $2\mu\text{m}$, $3\mu\text{m}$ is given

to the x, y, z-axis. These error values are assumed to be close error values present in the actual device.

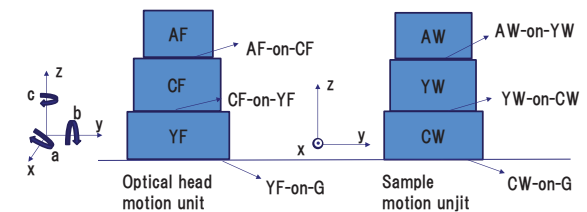


FIGURE 4. Schematic view of the stage assembly order of nanoprofiler

Figure errors of the results gave each of the a-axis error $-1\mu\text{rad}$, $1\mu\text{rad}$ of the AF-on-CF are shown in the figure 4. Correction of the first order has been added to these results. For errors of the first order is the slope, it does not affect the shape of the sample itself. Error of the first order is ignored for that reason. Distribution of figure error is such as represented by the cubic function of the distance r from the center of the sample. Distribution of figure errors are the same in Figure. 4(a), (b), the result of positive and negative values are swapped was obtained. Plot of a PV value of the figure error in giving an error in the a-axis of the AF-on-CF is shown in FIG.5. Effect of assembly errors have on the figure error is linear. Due to the evaluation by the PV value, PV value is bent in respect of assembly error is 0, positive and negative values of the distribution are interchanged as shown in FIG.4. And that the distribution is not changed by the increase or decrease of assembly errors, the influence of the assembly error is linear is confirmed. This trend is the same as all error of 6 degrees of freedom of assembly of six, in total 36 parameters. With this property, it may be possible to estimate the assembly error from the experimental value of the figure error.

Results gave $1\mu\text{rad}$ assembly error, each of a, b, c axis and $1\mu\text{m}$ assembly error, each of x, y, z axis AF-on-CF in Figure 7 is shown. The distribution substantially the same is shown in the a, c, x, z-axis. This trend is similar in the assembly of all six. The results in b and y-axis shows the distribution different from the others. It has a distribution that both can be expressed by a quadratic function.

It is as shown in FIG 8 is result of adding assembly errors b, and y-axis assembly CF-on-YF, YF-on-G, the CW-on-G. Distribution is a quartic function in the b-axis, is a quadratic function in the y-axis.

It is as shown in Figure 9 is the result obtained by adding the assembly errors b, and y-axis assembly AW-on-YW, YW-on-CW. It has a distribution of the quadratic functions both b, y - axis. The manifestation of figure error, these are influenced by the complex behavior of each axis. To reveal the regularity of the effect of assembly error on the figure error is the task of the future. Next, a table of the sensitivity of the error in each is shown in Table 1. Sensitivity is a value corresponding to the slope of the plot shown in FIG.6. The value of the sensitivity of the assembly error in the y-axis is larger by one order of magnitude compared to the others. That the error in the y-axis is the dominant influence of assembly error is confirmed. For the actual device, y-axis is uncertain most, so errors in the y-axis is considered to be corrected by the autonomic calibration. By using the information on the sensitivity and distribution of figure error revealed, comparison of experiment and simulation is performed to estimate the assembly error of the device in the future. In addition, measuring the assembly errors of the apparatus, a comparison between the estimated value and verify the estimation of assembly errors using simulation.

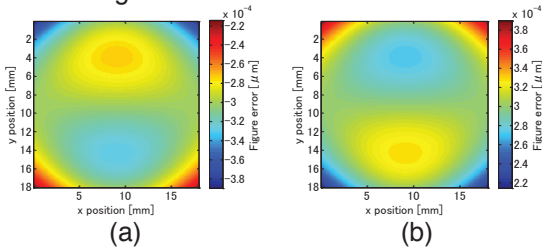


FIGURE 5. Figure errors under the influence of assembly error in a-axis of AF-on-CF (a) $-1\mu\text{rad}$ error, (b) $1\mu\text{rad}$ error

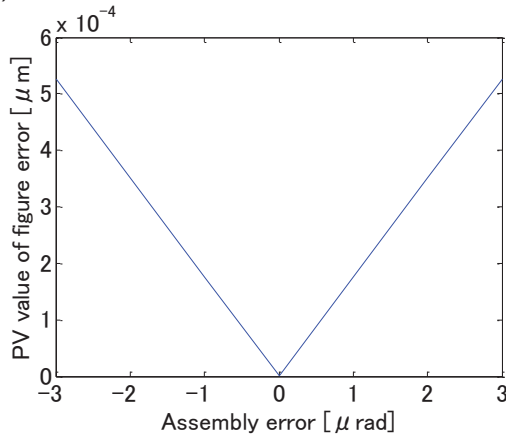


FIGURE 6. Linear change of PV value of figure errors under the influence of assembly error in a-axis of AF-on-CF

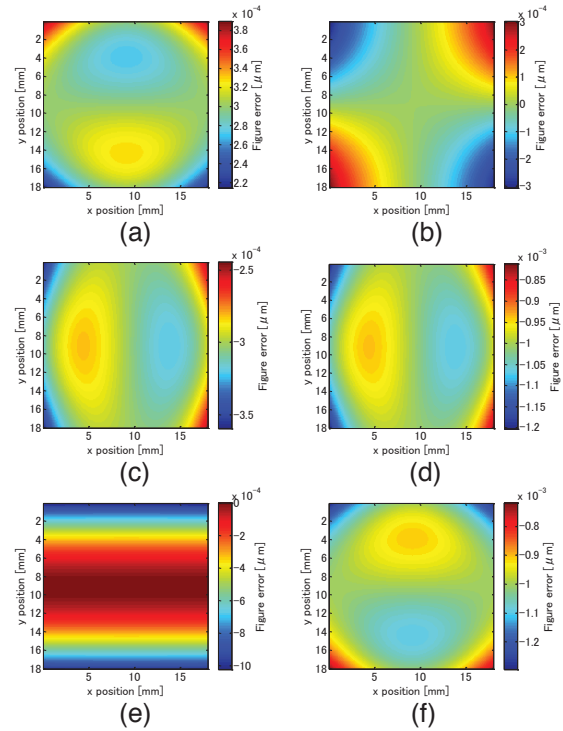


FIGURE 7. Figure errors under the influence of assembly error of six-degrees freedom into AF-on-CF (a) a-axis, (b) b-axis, (c) c-axis, (d) x-axis, (e) y-axis, (f) z-axis

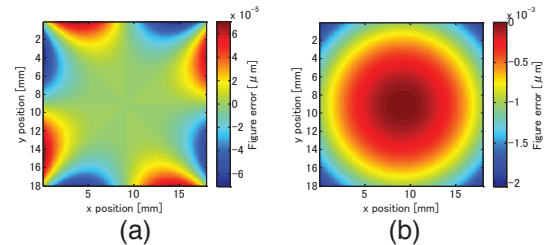


FIGURE 8. Figure errors under the influence of assembly error in b-axis or y-axis of CF-on-YF, YF-on-G, the CW-on-G (a) b-axis, (b) y-axis

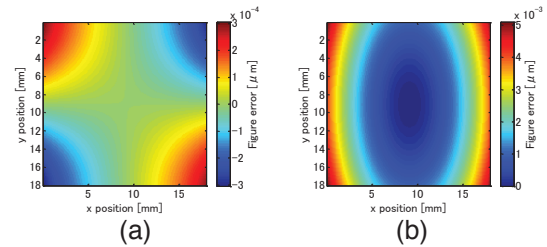


FIGURE 9. Figure errors under the influence of assembly error in b-axis or y-axis of AW-on-YW, YW-on-CW (a) b-axis, (b) y-axis

TABLE 1. The sensitivity of assembly error

	a[nm/μrad]	b[nm/μrad]	c[nm/μrad]	x[nm/μm]	y[nm/μm]	z[nm/μm]
AF-on-CF	1.75E-01	6.08E-01	1.15E-01	3.91E-01	1.013	5.78E-01
CF-on-YF	1.81E-01	1.42E-01	1.15E-01	3.61E-01	2.02	5.78E-01
YF-on-G	1.96E-01	1.42E-01	1.30E-01	3.61E-01	2.02	5.78E-01
AW-on-YW	1.99E-02	6.07E-01	1.40E-02	1.99E-01	5.045	5.78E-01
YW-on-CW	1.99E-02	6.07E-01	1.40E-02	1.99E-01	5.045	5.78E-01
CW-on-G	3.57E-02	1.42E-01	1.40E-02	3.61E-01	2.02	5.78E-01

CONCLUSION

Nanoprofiler that we have developed has good reproducibility, is not well understood uncertainty. As a first step for verification of uncertainty of device, by using a numerical simulation, it was investigated the effect of systematic errors. By adding the assembly errors of six degrees of freedom to the stage assembly of six, it is a simulation reflecting all the stage assembly error. As a result, the following was found.

1. By increase or decrease of the assembly error, the shape of the distribution figure error does not change.
2. The effect of assembly error increases or decreases appears as a linear increase or decrease of figure error.
3. When given a assembly error a, c, x, and z-axis, figure error distribution is a distribution that is represented by a cubic function.
4. When given a assembly error in the b-axis, figure error distribution is a distribution that is represented by a fourth-order function or a quadratic function.
5. When given the assembly errors in the y-axis, figure error distribution a distribution represented by a quadratic function.
6. The sensitivity of assembly error is greatest in the y-axis.

It is assumed estimate assembly error due to these findings, comparing the figure error and the simulated shape errors obtained in the experiment is made possible.

In the future, we will carry out the following issues:

1. Analysis is advanced for systematic errors are not taken into account this paper.
2. Estimation of systematic error is attempted, and the estimated value is compared with the measured values of systematic errors of device.

3. Development of self-calibration method for the correction of the y-axis translation error proceeds.

It is contemplated that the uncertainty of the nanoprofiler found the achievement of these and reduce the uncertainty even as possible.

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Evaluation of manufacturing tolerance for a model ship hull by using stitched stereo vision data sets

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ABSTRACT

As various hull forms are developed, the importance of manufacturing accurate model ship for performance test has increased. Generally, manufacturing accuracy of the model ship has been evaluated by CMM (coordinate measuring machine). However, traditional evaluating method takes long time due to measuring only single points on CMM. And it is difficult to obtain data with high resolution and to configure the freeform surface of a ship's bow and stern.

A research was conducted, using 3-dimensional stereo vision measuring instrument, to collect the accurate data measurement of freeform surface. An optical 3-dimensional measurement technology is known to be fast and accurate research method. However, relatively small single-measurement area of 7 m length enabled us to evaluate only limited portion of the model. Therefore, the process to stitch each data from different parts is required. When stitching 3-dimensional data, first step is to mark reference points before any process. Then, based on standard points, data is adjusted into one piece. However, the data value changes from filtering. Therefore, this method is not suitable in some cases such as determining the manufacturing tolerance, which needs every data estimate to be accurate, to meet the standard of the International Towing Tank Conference (ITTC).

In this research, data stitching algorithm which only requires measured data and no reference points is proposed. The test was conducted to confirm if the algorithm can gather precise and detailed results during measuring model ship. The evaluation of the manufacturing tolerance of model ship was done using comparison of real design data and stitching data.

INTRODUCTIONS

Recently, EEDI (Energy Efficiency Design Index)'s regulations have put the ship's efficiency on the highest interest for those in the ship-building industry. Thus, research on various shapes of hull, one of the factors to develop ship's performance, is being conducted actively.

The self-propulsion test of a hull is performed using a model ship under the law of similarity in the towing tank. Towing tank is formed very similarly to the actual marine environment. When evaluating performance of the ship, the proportion of ship to ship's model and its correlation coefficient are taken into account based on the measured results by towing tank test. Model ship of test results to expand the performance of real ship has to be followed within 1mm in term of manufacturing tolerance which regulated by ITTC.

The evaluation of the model's manufacturing tolerance which is an important factor measuring the performance in the test, is mandatory requirement. Currently, evaluating machining accuracy of the model ship is being carried out using CMM (coordinate measuring machine). A person directly measures using stylus of CMM. However, it takes a long time and has low-resolution data.

In the purpose to improve resolution and to shorten the duration, the stitching method—stitching data from each small surface using the 3-dimensional vision machine—is being studied [1-3].

However, it is difficult to select a feature point when using the stitching method due to the featureless characteristic of the free-form surface model ship. Furthermore, the previous stitching algorithm which has used a filter to collect data, results in the loss of actual

measurement data and makes the experiments difficult.

In this study, another stitching method, using the error correction among the measurement data without any feature point, was presented. This method, besides, does not generate loss of actual data because it only uses the measurement data without filtering. In fact, this approach has been verified to give accurate result with the design data through the algorithm test with a model ship.

MEASUREMENT OF MODEL SHIP USING 3-DIMENSIONAL VISION MACHINE

In this study, bow and stern of the model ship was measured that were in free curved surface shape. Three-dimensional stereo vision measurement system which operates with 1-projector and 2-cameras was used.

Figure1 demonstrates model ship measurement picture taken by the stereo camera. Active stereo vision system was used (Korea, OnScan TU-50).

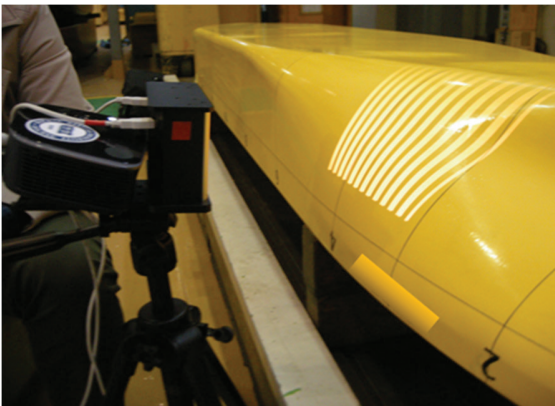


Figure 1 Measurement of model ship using stereo camera

After moving the measurement system translational across x and y axes by 8cm equally, Model ship was measured using single measuring size (12cm x12cm) as the standard.

Each bow and stern were divided by 10 pieces horizontally and 2 pieces vertically, obtaining total of 40 pieces of data. Each adjacent measured data shared an overlap area of 4 cm x 12 cm (or 12 cm x 4 cm).

A single measurement data of the model was shown in figure 2.

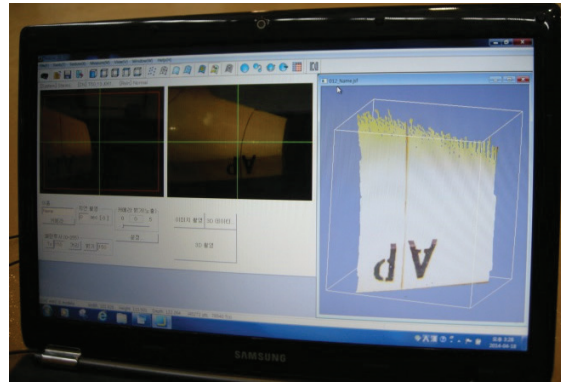


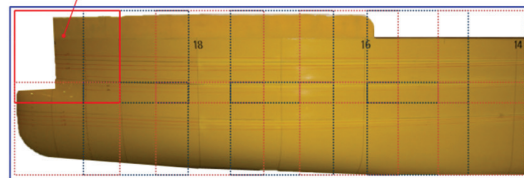
Figure 2 Single measurement data of model ship

5-D.O.F. ERROR CORRECTION STITCHING ALGORITHM

When measuring model ship, errors have occurred due to measuring machine translation and position. Thus, each measuring data contains position error of 6-d.o.f. Measurement data, after changing in x-axis (ΔX) and y-axis (ΔY), include x, y, z-axis translation errors (e_x , e_y , e_z) and rotational errors (θ_x , θ_y , θ_z) due to disturbances during the drive.

In order to solve the position error of 6-d.o.f., measuring surface required overlap area with adjacent surface, and by using least squares methods and correlation function calculation. Detailed information was extracted from overlap area, and conduct coordination by correcting position error based on detailed info [4-6]. Strategy of proposed 5-d.o.f. stitching method was shown in figure 3.

1st step: Acquisition of each measurement data



2nd step: Apply 5 D.O.F error stitching method

Figure 3 Strategy of the 5-D.O.F. error correction and stitching

However, generally, the rotational error of z-axis direction, θ_z , is relatively lower than other error components when the measurement takes surface through x and y-axis translation. Thus, the effect caused by this error component is determined to be minimal. For stitching of model ship, 5-d.o.f.errors correction was conducted.

Transversal translation error (e_x, e_y) was aligned in a measurement coordinate system for correcting. By applying all measurement data set to algorithm that can minimize cumulative error, θ_x , θ_y , and e_z were corrected. This process can be briefly observed in figure 4.

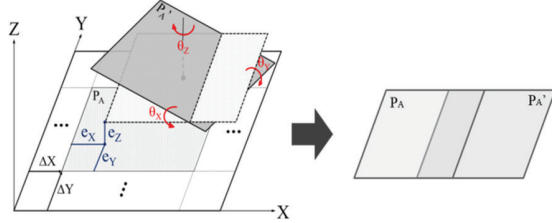


Figure 4 Schematic of 5-D.O.F. stitching process

P_A is single measured surface data. And P_A' is adjacent surface data on P_A . Transversal translation error that occurs during measurement uses shape data set in the same position among the overlap area data, and calculates for variance error using normalized cross correlation function. 2-dimensional normalized cross correlation function of two shape data extracted from each overlap area is expressed as formula (1).

$$c(u, v) = \frac{\sum_{x,y} [Z_{P_r}(x,y) - \bar{Z}_{P_r}] [Z_{P_t}(x-\Delta x, y-\Delta y) - \bar{Z}_{P_t}]}{\{\sum_{x,y} [Z_{P_r}(x,y) - \bar{Z}_{P_r}]^2 \sum_{x,y} [Z_{P_t}(x,y) - \bar{Z}_{P_t}]^2\}^{0.5}} \quad (1)$$

$c(u, v)$ represents the calculated cross-correlation coefficient. Z is the height data of the measurement surface.

Transversal translation error conjectured to be spatial lag that is expressed as the point in which cross correlation function shows the maximum value in horizontal incoordination amount of two profile data sets. Therefore, as seen in figure 5, the point was found at which cross correlation function has the maximum value within an area that had considered the driving error limits, and determine variance of error correction.

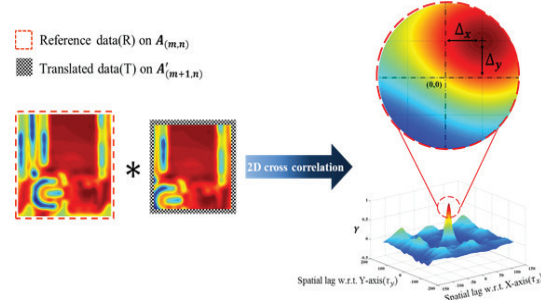


Figure 5 Normalized 2D cross-correlation function and estimated translation errors in the direction of X- and Y-axis

For x-axis rotational error, y-axis rotational error, and z-axis translation, least squares methods was used to solve for coefficients of each surface in which the sum of the height differences of the standard data and other data is minimum.

It was shown height difference of same position within overlap area using interaction formula, and using least squares methods for coefficient that minimizes this height difference. This is expressed in formula (2).

$$\sum (\{Z_n(x,y) - [Z_{n+1}(x-\Delta x, y) + ax + by + c]\}^2) \rightarrow \min(2)$$

By using least squares methods on all surface face, coefficients of each surface were solved in which the sum of the height differences of the standard data and other data is minimum. To easily calculate the coefficient values, matrix equation was expressed shown in formula 3 to 6 [7].

$$\left[\left(\sum_{k=0}^{tn-1} D_{ik} \right)_i \right] = \left[\left(Q_{ij} - \delta_{ij} \sum_{k=0}^{tn-1} Q_{ik} \right)_{ij} \right] [(s_i)_i] \quad (3)$$

$$\delta_{ij} = \begin{cases} 1 & \text{if } i = j \\ 0 & \text{if } i \neq j \end{cases}$$

$$D_{ij} = \begin{bmatrix} \sum_{i,j} x \Delta(x, y) \\ \sum_{i,j} y \Delta(x, y) \\ \sum_{i,j} \Delta(x, y) \end{bmatrix} \quad (4)$$

$$Q_{ij} = \begin{bmatrix} \sum_{i,j} x^2 & \sum_{i,j} xy & \sum_{i,j} x \\ \sum_{i,j} xy & \sum_{i,j} y^2 & \sum_{i,j} y \\ \sum_{i,j} x & \sum_{i,j} y & n_{ij} \end{bmatrix}, Q_{ij} = \begin{bmatrix} 0 & 0 & 0 \\ 0 & 0 & 0 \\ 0 & 0 & 0 \end{bmatrix} \quad (5)$$

$$S_i = \begin{bmatrix} a_i \\ b_i \\ c_i \end{bmatrix} \quad (6)$$

By applying this relation, even if the number of measured surfaces increases by using matrix transformation of least squares methods. Error correction value could be solved efficiently. And cumulative error could be inhibited.

Finally, by removing error factors included in each measurement data, coordination between data was conducted.

TRANSLATION DATA FORMAT BY USING DATA INTERPOLATION

The 3-dimensional measurement using vision machine helps shortening the duration. On the other hand, however, the shape of data is inconsistent and even some gaps of data appear. This inconsistency of measured data makes it difficult to apply stitching algorithm which gathers error-corrected data from each measured pixel. Therefore, the interpolation algorithm is conducted. This method adjusted measured data into a regular interval and generated data those not measured by using adjacent points.

The spline interpolation method and data processing method was used. Figure 6 shows a comparison of data before and after the interpolation.

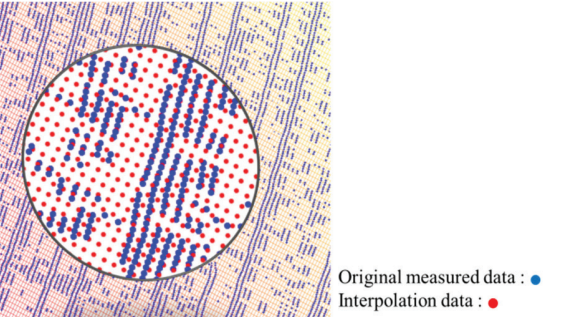


Figure 6 Measurement data type conversion through data interpolation

Through the interpolation, all measured data are converted into the uniform size. In addition a consistent data set, including all pixel values, is created. The results from this data interpolation are, for data stitching, follow by applied to the correction of errors at 5-d.o.f.

STITCHING ALGORITHM APPLIED TO THE MEASURED DATA

They are as follows: The result of applying a stitching algorithm of the data after interpolation.

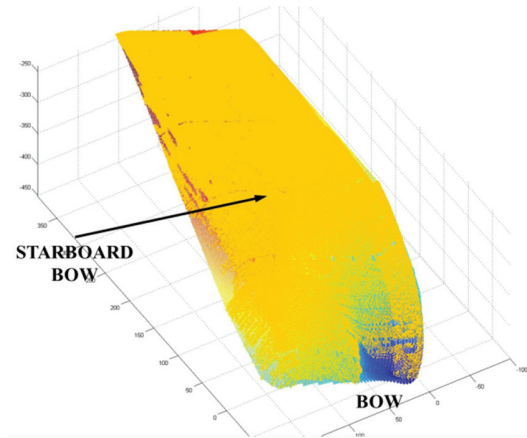


Figure 7 Final stitching data of model ship's bow

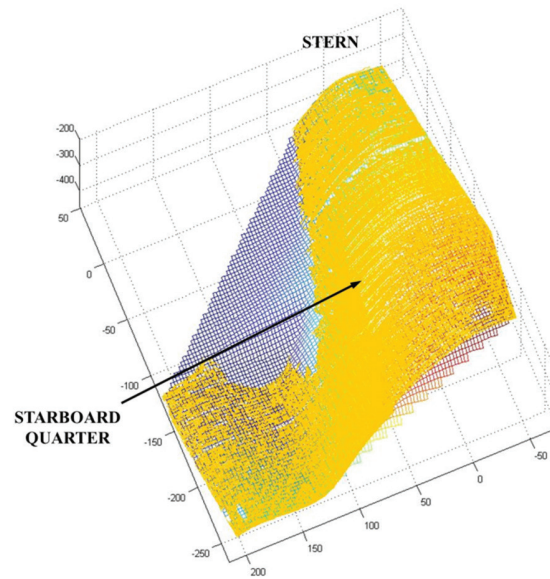


Figure 8 Final stitching data of model ship's stern

To ensure the reliability of the stitching algorithm, RMS (root mean square) value of the error was determined between the design data and the stitching data. The following is a picture of a two-dimensional actual model ship design.

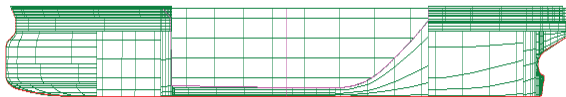


Figure 9 The model ship design data

The following table shows results of error RMS.

TABLE 1. Results of error RMS.

	RMS
BOW	0.25 mm
STERN	0.38 mm

Bow's RMS result was 0.25 mm. Stern's RMS result was 0.38 mm. As a result, it was verified that the data which was calculated through algorithm had an average error of 0.32 mm.

However, this value did not include the machining error of model ship. Accordingly, stitching error by proposed algorithm was expected smaller than 0.32 mm.

Additionally, it should be in progress for the evaluation of the stitching algorithm error. Uncertainty of stitching algorithm is going to be calculated by using high resolution specimen. In addition the stitching error between high precision measuring data (measured by interferometer) and stitching data will be calculated.

CONCLUSION

In this research, 5-degrees of freedom error correction algorithm was applied to measuring a model ship with a free-form surface.

Model ship's stern and bow were measured in a single area of the size of 12 cm x12 cm to have a common area of 30% of adjacent surfaces. Measurement was used by the 3-dimensional active stereo vision system.

The measured data was applied to the stitching algorithm after changing the data type by using the spline interpolation.

After obtaining the stitching measurement data, machining accuracy of the model ship was confirmed. The RMS error values between the design data and the stitching data was calculated, and the reliability of algorithm was ensured.

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Modeling and analysis of a heat engine with coil-spring-shaped SMA for steady state performance and starting condition

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INTRODUCTION

Recently, there are various micromanipulation technologies [1,2,3]. However, they need wide area compared with a size of their micro targets. They are not able to manipulate large amount of micro objects simultaneously and individually because those micromanipulation have done by an outside forces such as optical trapping forces and electromagnetic forces. In many years, people are desired to develop micro robots, less than 1mm^3 , which move freely inside bio-medical bodies and blood vessels. If we can develop the micro robots whose sizes are less than 1mm^3 , it has high usability because several robots can manipulate several objects simultaneously and individually. They can carry medicine and/or examine directly at disease parts on-site. To move numerous micro robots simultaneously and individually, we need add energy harvester and self-propulsion capability to the micro robot. However, there are two main problems. One is “sticky problem”, the micro robot are easily trapped by adhesive forces, such as electrostatic force, interfacial force, and viscous drag force which are normally negligible over cm-sized objects. Another is energy source problem because it is very hard to embed power source with sufficient energy density in only 1mm^3 -sized body. Therefore, we believe the micro robot should harvest energy from environment. In the natural world, there are many microbes lived in liquid, such as water flea and paramecium. They move freely with harvesting energy by eating foods and absorbing solar energy in liquid. Those microbes have already solved sticky problem because electrostatic force and interfacial force are dampen very well in a liquid and do not trap microbes at all. That is the reason why we believe we should move the micro robot in a liquid. Conventional studies of the micro robot

have categorized flying-type in the air [4], floating-type on the water surface, and swimming-type in a liquid [5,6]. Last year we have designed and analyzed unique design of the in-liquid micro robot that is composed of a voltaic cell as an energy harvester and a comb actuator as a source of motion [7]. However we have concluded we need over 2000V to move the in-liquid micro robot with velocity of 0.1mm/s ; 2V of the voltaic cell is not sufficient as the energy source for the comb actuator. Recently we have focused on a heat engine with Shape Memory Alloy actuator (SMA actuator) with coil-spring characteristics [8,9]. In this article, we have described the heat engine that is composed of SMA actuator and two pulleys and three gears. We have also calculated the motion of the heat engine at the steady state and formulate the starting condition.

Design of the heat engine

We have designed the heat engine using expansion and contraction of SMA actuator by temperature change of SMA actuator in Figure.1. The size of the heat engine is $35\text{mm}\times 60\text{mm}\times 4.5\text{mm}$. Gear B is an idle gear which makes gear A and gear C rotate same direction. Gear A and pulley A is fixed; we assume rigid body A is composed of them. In a similar way, we assume rigid body C is composed of gear C and pulley C.

Moving principle of the heat engine

When the SMA actuator in hot area shrinks, its pulling forces to gear A and C are same. However, the generative torque of gear A is smaller than that of C because the pitch radius of gear A is smaller than that of C. The balance of torque that acts of gear B breaks and gear A rotates the direction that gear C is going to rotate.

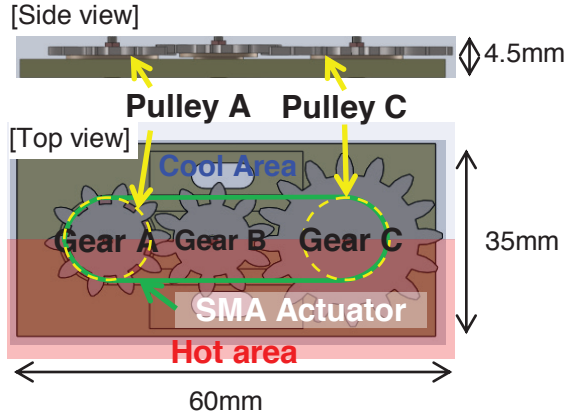


FIGURE 1. Structure of the heat engine

Force acted on gears and rigid bodies

We describe forces and torques acted on rigid body A, C and B in Figure 2 .

Here, the parameters are defined as below,

r_A, r_B, r_C : a pitch radius of gear A, B, and C
 R : a radius of pulley (all pulley's radii are same)
 I_A, I_B, I_C : moment of inertia of A, B, and C
 T_{RA}, T_{RB}, T_{RC} : Torque by external force of A, B, and C including the torque generated by friction force.

F_{HA}, F_{HC} : Component of x axis of force vector acted on rigid body A and C by SMA in hot area.

F_{LA}, F_{LC} : Component of x axis of force vector acted on rigid body A and C by SMA in cool area.

f_{BA}, f_{AB} : forces acted on A from B and B from A

f_{CB}, f_{BC} : forces acted on B from C and C from B

$\vec{f}_{ax_A}, \vec{f}_{ax_B}, \vec{f}_{ax_C}$: a force vector acted on rigid bodies A, C, and B from their own rotational axes.

These forces work as binding forces because the total force of x-axis, y-axis acted on A, B, and C should be zero.

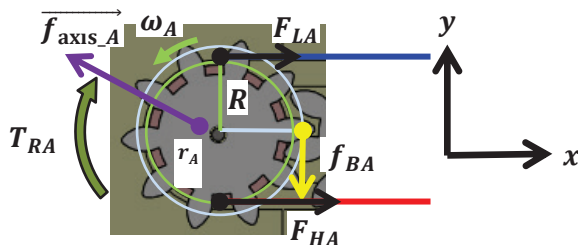


FIGURE 2. Forces and torques acted on A

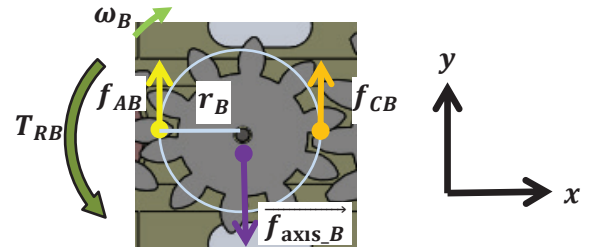


FIGURE 3. Forces and torques acted on B

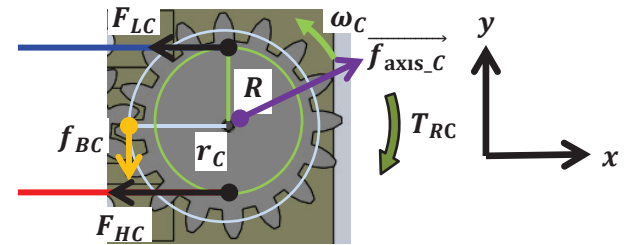


FIGURE 4. Forces and torques acted on C

from Figure.2, 3, 4, equation of motion about the center of rotation is,

$$I_A \dot{\omega}_A = R(F_{HA} - F_{LA}) + r_A f_{BA} + T_{RA} \quad (1)$$

$$I_B \dot{\omega}_B = -r_B f_{AB} + r_B f_{CB} + T_{RB} \quad (2)$$

$$I_C \dot{\omega}_C = R(F_{HC} - F_{LC}) - r_C f_{BC} + T_{RC} \quad (3)$$

from inertial force of rigid body A, C and B,

$$\begin{bmatrix} F_{HC} \\ F_{LC} \\ f_{AB} \\ f_{CB} \end{bmatrix} = - \begin{bmatrix} F_{HA} \\ F_{LA} \\ f_{BA} \\ f_{BC} \end{bmatrix} \quad (4)$$

Gear A, B, and C is binded by the condition of rolling contact as,

$$r_A \omega_A = -r_B \omega_B = r_C \omega_C. \quad (5)$$

When we differentiate equation (5) with respect to time, we get

$$r_A \dot{\omega}_A = -r_B \dot{\omega}_B = r_C \dot{\omega}_C. \quad (6)$$

From equation(6), if angular accceleration of gear A or B or C is zero, angular acceleration of other two gears is zero too.

Steady state analysis

Here, we investigate the motion in steady state. We define a condition of the steady state as,

Assumption 1: Angular velocity of gear A, B, and C are constant.

from assumption1,

$$\dot{\omega}_A = \dot{\omega}_B = \dot{\omega}_C = 0 \quad (7)$$

from equation(1),(2)(3),(4),(7),

$$f_{AB} = \frac{1}{r_B} T_{RB} - \frac{1}{r_C} T_{RC} + \frac{1}{r_C} R(F_{HA} - F_{LA}) \quad (8)$$

$$f_{CB} = -\frac{1}{r_C} T_{RC} + \frac{1}{r_C} R(F_{HA} - F_{LA}) \quad (9)$$

$$T_{RA} = -R(F_{HA} - F_{LA}) \left(1 - \frac{r_A}{r_C}\right) + \frac{r_A}{r_B} T_{RB} - \frac{r_A}{r_C} T_{RC} \quad (10)$$

Here, T_{OA} is an output torque acted to outside from A,

$$T_{OA} = -T_{RA} \quad (11)$$

futhermore, we define W_A as work efficiency of output torque T_{OA} as,

$$W_A = T_{OA} \cdot \omega_A. \quad (12)$$

Here, we asuume geometrical assumption as,

Assumption 2: Pitch radius of gear A is smaller than that of gear C

$$r_A < r_C \quad (13)$$

we define F_H as an absolute value of F_{HA} in hot area and F_L is absolute value of F_{LA} in cool area.

$$F_H = F_{HA} > 0 \quad (14)$$

$$F_L = F_{LA} > 0 \quad (15)$$

Assumption 3: F_H is larger than F_L ; F_H minus F_L (ΔF) is larger than 0

$$\Delta F = F_H - F_L > 0 \quad (16)$$

from equation(10),(16), equation(11) is

$$T_{OA} = R\Delta F \left(1 - \frac{r_A}{r_C}\right) - \frac{r_A}{r_B} T_{RB} + \frac{r_A}{r_C} T_{RC} \quad (17)$$

and from equation(17), equation(12) is

$$W_A = \left\{ R\Delta F \left(1 - \frac{r_A}{r_C}\right) - \frac{r_A}{r_B} T_{RB} + \frac{r_A}{r_C} T_{RC} \right\} \cdot \omega_A \quad (18)$$

We assume the following approximation to consider the influence of torque by external force.

Assumption 4: T_{RA} is the sum of dynamic frictional torque T_{KFA} and viscous torque.

The viscous torque is proportional to the angular velocity ω_A and acts to the reverse direction of ω_A .

here, the parameters are defined as,

T_{KFA} , T_{KFB} , T_{KFC} : Maximum dynamic friction torques acted from each rotational axes

C_A , C_B , C_C : Coefficients of the viscous torques
The torques acted from the external forces is represented as,

$$\begin{bmatrix} T_{RA} \\ T_{RB} \\ T_{RC} \end{bmatrix} = - \begin{bmatrix} C_A \omega_A \\ C_B \omega_B \\ C_C \omega_C \end{bmatrix} + \begin{bmatrix} T_{KFA} \\ T_{KFB} \\ T_{KFC} \end{bmatrix}. \quad (19)$$

From equations of (11), (19),

$$\omega_A = \frac{1}{C_A} (T_{KFA} + T_{OA}). \quad (20)$$

When steady state, $T_{OA} > 0$, $T_{KFA} < 0$ must be true.
So, from equation (20), ω_A should be larger than zero,

$$T_{OA} > -T_{KFA} (> 0) \quad (21)$$

From equation (21), output torque T_{OA} is larger than the dynamic viscous torque of T_{KFA} .

If equation (21) is true, from equation (17) and (20),

$$\omega_A = \frac{1}{C_A} \left\{ R\Delta F \left(1 - \frac{r_A}{r_C}\right) + r_A \left(-\frac{1}{r_B} T_{RB} + \frac{1}{r_C} T_{RC}\right) + T_{KFA} \right\} \quad (22)$$

From equation (20) and (12) is

$$W_A = T_{OA} \cdot \frac{1}{C} (T_{KFA} + T_{OA}) \quad (23)$$

Equation (23) means working efficiency W_A is larger than zero if equation (21) is true.

Starting codition

It is necessary that generative torque of the heat engine is larger than the torque by maximum static frictional torque T_{SFA} , T_{SFB} , T_{SFC} to start the rotation of the heat engine. At the initial condition of the heat engine, velocity is zero and viscous force is zero. Therefore, viscous torque does not influence the starting condition. After starting, maximum static friction torque becomes dynamic frictional torque T_{KFA} , T_{KFB} , T_{KFC} . As shown in figure 5, we describe forces and torques acted on rigid body A, C and B when the moment of starting in static state,

Where, the parameters are defined as,

T_{SFA} , T_{SFB} , T_{SFC} : Maximum static friction force acted from each rotational axis to A, B, and C.

r_O : Radius of shaft (all shaft radii is same)

μ' : Coefficient of dynamic friction between each rotational axis and A, B, and C.

μ : Coefficient of static friction between each rotational axis and A, B, and C.

$\vec{F}_{NA}$, $\vec{F}_{NB}$, $\vec{F}_{NC}$: Normal force acted on A,B,C from each rotational axis.

θ_A , θ_B , θ_C : Angles between $\vec{F}_{NA}$, $\vec{F}_{NB}$, $\vec{F}_{NC}$ and $\vec{f}_{ax_A}$, $\vec{f}_{ax_B}$, $\vec{f}_{ax_C}$

$\vec{F}_{SFA}$, $\vec{F}_{SFB}$, $\vec{F}_{SFC}$: Maximum static friction force vector acted on A, B, and C

$\vec{F}_{KFA}$, $\vec{F}_{KFB}$, $\vec{F}_{KFC}$: Dynamic friction force vector acted on A, B, and C

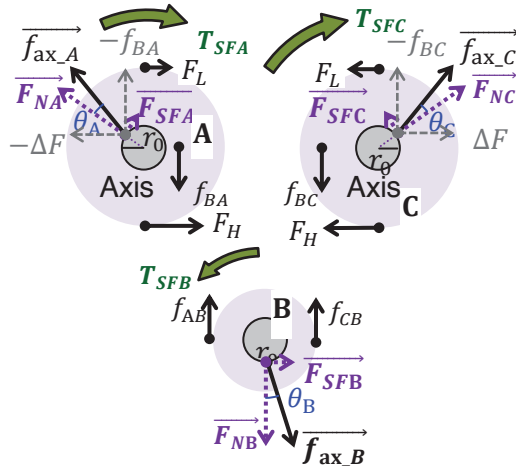


FIGURE 5. Forces and torques acted on A, B, and C in the moment of starting

here, we assume

Assumption 5: Coefficient of friction μ and μ' is much smaller than 1

Therefore,

$$\begin{aligned} \theta_A &\ll 1 \\ \theta_B &\ll 1 \\ \theta_C &\ll 1 \end{aligned} \quad (24)$$

from equation(24) and figure 5, we can approximate $\vec{F}_{NA}$, $\vec{F}_{NB}$, $\vec{F}_{NC}$ equals to $\vec{f}_{ax_A}$, $\vec{f}_{ax_B}$, $\vec{f}_{ax_C}$ respectively as,

$$\begin{bmatrix} \vec{F}_{NA} \\ \vec{F}_{NB} \\ \vec{F}_{NC} \end{bmatrix} \cong \begin{bmatrix} \vec{f}_{ax_A} \\ \vec{f}_{ax_B} \\ \vec{f}_{ax_C} \end{bmatrix} \quad (25)$$

from equation(25), we can approximate maximum static friction force and dynamic friction force.

$$\begin{bmatrix} |\vec{F}_{SFA}| \\ |\vec{F}_{SFB}| \\ |\vec{F}_{SFC}| \end{bmatrix} \cong \mu \begin{bmatrix} |\vec{f}_{ax_A}| \\ |\vec{f}_{ax_B}| \\ |\vec{f}_{ax_C}| \end{bmatrix} \quad (26)$$

$$\begin{bmatrix} |\vec{F}_{KFA}| \\ |\vec{F}_{KFB}| \\ |\vec{F}_{KFC}| \end{bmatrix} \cong \mu' \begin{bmatrix} |\vec{f}_{ax_A}| \\ |\vec{f}_{ax_B}| \\ |\vec{f}_{ax_C}| \end{bmatrix} \quad (27)$$

from equation(26) and figure 5, maximum static torques acted from each rotating axis are

$$\begin{bmatrix} T_{SFA} \\ T_{SFB} \\ T_{SFC} \end{bmatrix} = \mu r_o \begin{bmatrix} -\sqrt{\Delta F^2 + f_{AB}^2} \\ f_{AB} + f_{CB} \\ -\sqrt{\Delta F^2 + f_{CB}^2} \end{bmatrix} \quad (28)$$

from equation(1) to (3) and (28), At resting state just before the starting, following equilibrium of torques should be true as,

$$R\Delta F - r_A f_{AB} = \mu r_o \sqrt{\Delta F^2 + f_{AB}^2} \quad (29)$$

$$r_B f_{AB} - r_B f_{CB} = \mu r_o (f_{AB} + f_{CB}) \quad (30)$$

$$R\Delta F + r_C f_{CB} = \mu r_o \sqrt{\Delta F^2 + f_{CB}^2} \quad (31)$$

from equation(29), (31)

$$f_{AB} = p\Delta F \quad (32)$$

$$f_{CB} = q\Delta F \quad (33)$$

Where,

$$\begin{bmatrix} p \\ q \end{bmatrix} \equiv \begin{bmatrix} a - \sqrt{a^2 - b} \\ c - \sqrt{c^2 - d} \end{bmatrix} \quad (34)$$

$$a \equiv \frac{Rr_A}{r_A^2 - \mu^2 r_o^2} \quad (35)$$

$$b \equiv \frac{R^2 - \mu^2 r_o^2}{r_A^2 - \mu^2 r_o^2} \quad (36)$$

$$c \equiv \frac{Rr_C}{r_C^2 - \mu^2 r_o^2} \quad (37)$$

$$d \equiv \frac{R^2 - \mu^2 r_o^2}{r_C^2 - \mu^2 r_o^2} \quad (38)$$

For simplification, we assume following approximation

Assumption 6: Product of coefficient of friction μ and radius of shaft r_o is much smaller than radius of the gear A.

$$r_A \gg \mu r_o \quad (39)$$

from equation(39), we can approximate p and q by ignoring second dimension of μr_o as,

$$\begin{bmatrix} p \\ q \end{bmatrix} \cong \begin{bmatrix} \frac{R}{r_A} - \frac{\mu r_O}{r_A} \sqrt{1 + \frac{R^2}{r_A^2}} \\ \frac{R}{r_C} - \frac{\mu r_O}{r_C} \sqrt{1 + \frac{R^2}{r_C^2}} \end{bmatrix}. \quad (40)$$

from equation(6), if one of the angular acceleration ω_A , ω_B , and ω_C is not zero, other two angular accelerations are also not zero.

$$R\Delta F - r_A f_{AB} > \mu r_O \sqrt{\Delta F^2 + f_{AB}^2}, \quad (41)$$

$$r_B f_{AB} - r_B f_{CB} > \mu r_O (f_{AB} + f_{CB}), \quad (42)$$

$$R\Delta F + r_C f_{CB} > \mu r_O \sqrt{\Delta F^2 + f_{CB}^2}. \quad (43)$$

in this article, we concentrate on equation(42), equation(42) is

$$r_B(p - q) > \mu r_O(p + q). \quad (44)$$

from equation(40),(44), we can get this condition.

$$\frac{r_B}{r_O} \cdot \frac{1 - \frac{r_A}{r_C}}{1 + \frac{r_A}{r_C} + \frac{r_B}{R} \sqrt{1 + \frac{R^2}{r_A^2}} - \frac{r_A \cdot r_B}{R \cdot r_C} \sqrt{1 + \frac{R^2}{r_C^2}}} > \mu \quad (45)$$

from equation(45), starting condition is not related to the force difference of SMA ΔF but only related to coefficient of friction μ , pitch radius of gear A, B, and C r_A, r_B , and r_C , radius of pulley R and radius of shaft r_O .

Future works

We plan to study coupled analysis using dynamic and thermodynamics model to investigate more detailed performance of the heat engine. Furthermore, we will investigate wireless energy harvesting methods of the heat engine to investigate the feasibility of the heat engine as a source of motion of the micro robot in a liquid.

Acknowledgement

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ANALYSIS OF INDUCTIVE-TYPE BRUSHLESS RESOLVER WITH R/D CONVERTER

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INTRODUCTION

Resolvers are electromagnetic sensors based on electromagnetic circuits for detection of angular displacement and velocity in motor control applications. Although the accuracy of angle detection in resolvers is inferior to optical encoders, resolvers are still widely employed for feedback sensors in aerospace, military, and industrial equipment because of outstanding mechanical ruggedness and superior ability of noise rejection. Furthermore, resolvers are relatively cost-effective and capable of sustaining severe environmental conditions. With a resolver mounted on the rotating shaft of a motor, it generates two carrier-modulated sinusoidal voltage output signals relevant to the rotor angle; the signals are then connected to a demodulation circuit, namely Resolver to Digital Convertor (RDC), in which the carrier signals are removed with digital angular velocity and displacement data posted at output ports.

The objectives of this paper are to enhance the decoding accuracy by systematic simulation of a complete resolver system and analyze pertinent parameters that influence the dynamic performance and accuracy of the angular displacement data. The paper describes the contents in several parts. First, establishment of resolver parameters by a CAD package is given; then, import of the CAD model into a commercial electromagnetic field analysis program called Maxwell2D/3D copyrighted by ANSYS Inc. are presented. With the optimetrics feature in the package, optimal design parameters are searched for performance indexes based on output signals. Finally, complete design with rotary transformer being modeled as equivalent circuit model are determined. By coding the decoder model based on the RDC into a dynamic system simulation program called MatLab copyrighted by the MathWorks Inc., MA U.S.A. Comprehensive study of the resolver system is illustrated with the multi-domain simulation features given by the software packages.

OPERATIONAL PRINCIPLE OF SYSTEM

Brushless resolvers can be considered as two magnetically coupled inductors mechanically bounded in relative motion. Tradition resolvers relies a wound rotor excited by ac voltage via slip-ring and brushes. In this paper, with consideration in the stringent requirements of reliability and accuracy, we choose rotary transformer instead of the brush and slip-ring. In the rotary transformer, the primary windings are in stator whereas the secondary windings are in rotor. The stator of brushless resolvers has two phase windings electrically spaced 90 degree from each other. One of the major differences between motors and resolvers is the turns of windings wound around each slot in resolver must be in sinusoidal distributions [1]. On the contrary, the motor windings in each slot are in equal turns. When an AC voltage is applied to the rotary transformer, the mutually coupled inductors between rotor and stator will generate the output signals in sine and cosine waveform simultaneously.

In model of ideal resolver, the voltage drops through the rotary transformer, wire resistance and magnetic interference between two phases are ignored. The Induced output voltages are described by following equations [2].

$$V_{\alpha} = L_r * \frac{di_{\alpha}}{dt} \quad (1)$$

$$V_D = L_{rsm} * \sin \delta * \frac{di_{\alpha}}{dt} \quad (2)$$

$$V_Q = L_{rsm} * \cos \delta * \frac{di_{\alpha}}{dt} \quad (3)$$

L_r : self-inductance of rotor coils

L_{rsm} : max. of mutual inductance between stator and rotor coils

δ : the current angle of the rotor

i_{α} : the excitation current

V_{α} : the excitation voltage

V_D : the sine output of position

V_Q : the cosine output of position

By assuming the ideal voltage source $V_\alpha = k_1 \cos(\omega t + \theta)$ for solving the current i_α with substitution into Eq. (2) and (3), the equations of output signal with the explicit position signal is described as follows.

$$V_D = k_2 \sin \delta \cos(\omega * t) \quad (4)$$

$$V_Q = k_2 \cos \delta \cos(\omega * t) \quad (5)$$

where ω is the rotating speed of rotor; and, both k_1 and k_2 are constant.

The signals will then be fed to the RDC circuits. First, the voltage signals in Eq. (4) and (5) are modulated by $\cos \varphi$ and $\sin \varphi$ respectively; where φ is the current estimated angular position of actual angular position δ , and then be subtracted. Hence, we get the difference in signals as follows.

$$V_{err} = V_D \cos \varphi - V_Q \sin \varphi = V_f \sin(\delta - \varphi) \cos(\omega * t)$$

The differential error signals are then demodulated simultaneously using the excitation signal as the demodulation reference. Finally, by using type II servo loop to drive V_{err} to zero, we approximate the instantaneous angular position which will be updated continuously.

RESOLVER MODEL AND ANALYSIS

The structure of the resolver in this research is determined as 2-pole with 12 stator slots and 20 rotor slots; and, the schematic drawing of 2D model is illustrated in Fig. 1. It is noted that in the stator winding coil-number should distributed for two phase output voltage in sine and cosine terms in orthogonal. The pertinent parameters of resolver are given in Table 1.

TABLE 1. Pertinent parameters of resolver

2pole , 11slots ^o		Unit mm ^o	
Outer diameter of stator ^o	34 ^o	Stator length ^o	3.5 ^o
Inner diameter of stator ^o	22.6 ^o	Rotor length ^o	3.15 ^o
Outer diameter of rotor ^o	22.2 ^o	Stator yoke ^o	3.4 ^o
Inner diameter of rotor ^o	13.8 ^o	Rotor yoke ^o	1.7 ^o

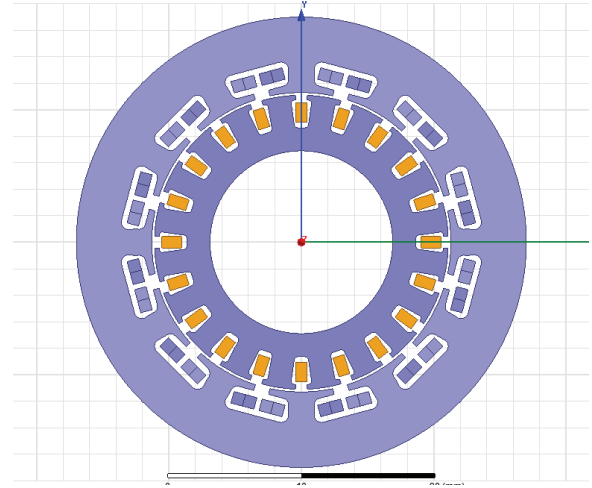


Figure 1. 2D resolver model with 2 pole 12 stator slot structure.

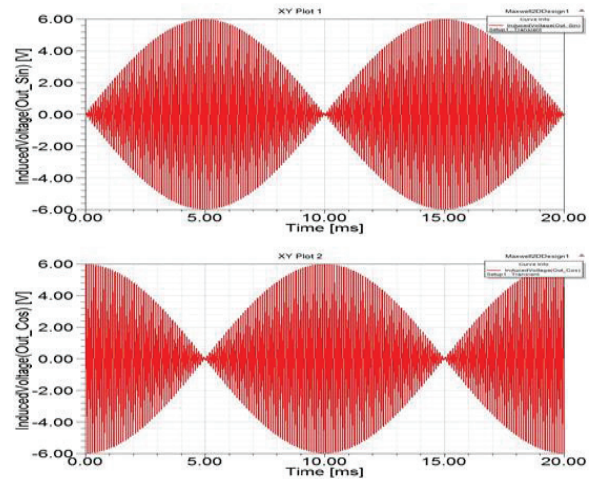


FIGURE 2. Simulated two phase output voltage of the 12 slot resolver design.

In the settings of analysis, input voltages should be at 7 Vrms and 10 kHz according to the electrical specifications. The time step is recommended to be not less than 10 μ s due to the 10 kHz excitation frequency. Plots of simulation results are shown in Fig. 2. In this study, various stator slots from 9 to 11 are also simulated. The results indicated that with the Fast Fourier Transform (FFT) and normalization the harmonic contents from the other 3 conditions are larger than the case of 12 slot design.

Another important factor that affects the overall accuracy in angular data is the amplitude difference in two phase windings [3]. For the

simulated case the output voltage being at 3 Volt, the difference in output voltage values are more than 0.3 Volt, i.e. close to 10 %. Therefore, a remedy was proposed with the alternating winding method [4]. By perturbing the winding sequences in each adjacent slot, the voltage difference could be reduced to 0.03 Volt, which decreases the compensation complexity in the decoder circuits. The normalized FFT results of sine phase output voltage after demodulation is shown in Fig. 3 with negligible harmonic contents.

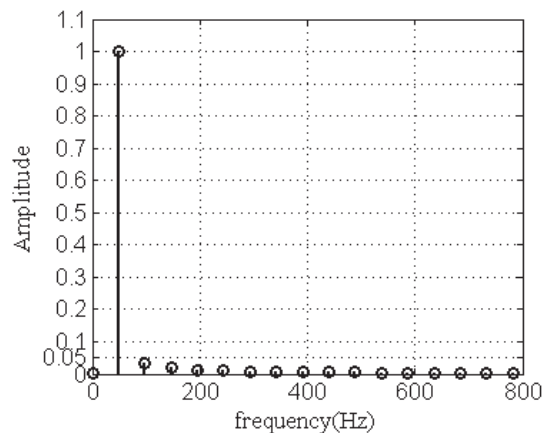


FIGURE 3. Normalized FFT results of resolver sinusoidal output voltage.

DECODER CIRCUITS

The functions of RDC are to demodulate the output signals of resolver and calculate the instantaneous angular displacement or rotational speed of a rotating shaft. The RDC circuits contain two demodulation blocks and one type-2 tracking servo loop block [5]. The block diagram represented in MatLab/Simulink language is shown in Fig. 4.

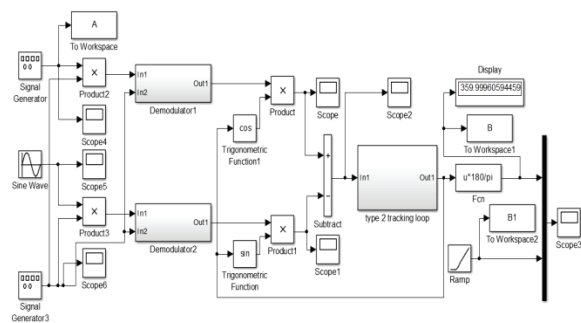


FIGURE 4. Block diagram of RDC circuits.

Signal sources in the figure are assumed to be ideal voltage generators because we want to investigate the calculated angular errors from the tracking loop. In the demodulators, signal resolution can be decided to be 10, 12, 14 or 16 bit. With resolution set at 12 bit and rotor rotational speed at 3,000 rpm, the tracking results are shown in Fig. 5. It is noted that the figure is blown-up to show how long the system takes to track the actual angular displacement; and, the complete plot should be 20 ms in X-axis and 360 degree in Y-axis. In this case, it takes 2 ms to completely follow; and, the error after one revolution is far below $0.6'$ with the average position error after transient being $0.9'$.

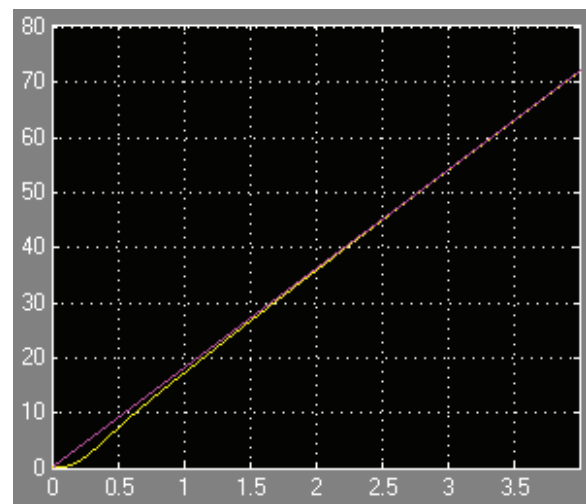


FIGURE 5. Plots of tracing situation with conditions of 12 bit, 3,000 rpm rotor speed.

In general, errors generated by resolver coupled with RDC is about $\pm 10'$, or ± 0.17 degree. The upper limit of angle detection error caused by RDC is then set to 0.2 degree. Figure 6 shows the various situations among step-size setup by users, rotational speed and RDC resolution. It meets the general conditions that require high resolution setting for low rotating speed and vice versa.

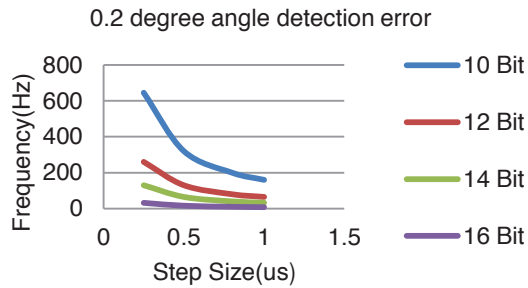


FIGURE 6. Conditions that cause 0.2 degree sensing error in corresponding to different step size and motor rotational speed.

from the resolver may seriously affect the accuracy of the system; hence, it should be compensated accordingly.

CONCLUSIONS

In this paper, components of the resolver angular displacement sensor system have been designed, modeled and simulated systematically. The components are then optimized by parametric analysis. Finally, the instantaneous dynamic signals are simulated with the accuracy of the system being verified with errors less than 0.15 degree, that is ± 9 minutes. Future works are needed to experimentally implement and

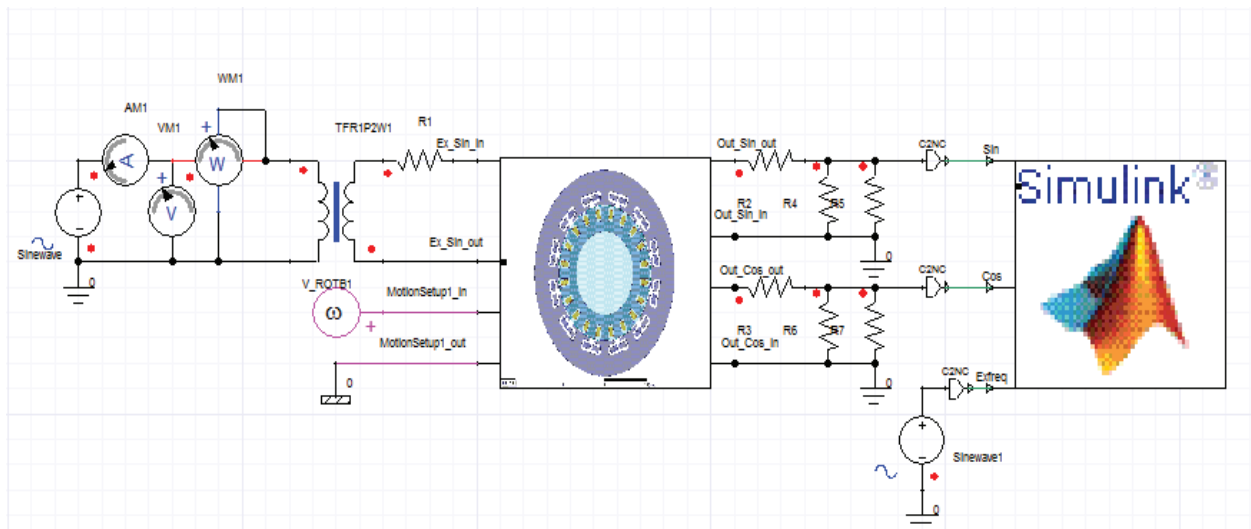


FIGURE 7. The sensing system which includes rotary transformer, resolver and resolver to digital converter (RDC).

CO-SIMULATION RESULTS

After the investigation of resolver and RDC, the models can be combined to simulate the dynamic rotational responses by multi-domain co-simulation programs. Figure 7 shows the complete diagram of the resolver system with Maxwell2D linked to Simulink by Simplorer. Rotary transformer attached to the resolver has been calculated with equivalent circuit model given by the results from Maxwell2D. The equivalent circuit model is obtained by computing the parameters of winding or Inductive reactance through standard open and short circuit tests. The output sensing angle error is 8.5 minutes after a revolution and it takes 5 ms to reach steady state. The detectable maximum rotational speed is over 10,000 rpm in this case. It should be noted that the phase shift between two voltage outputs

test the studied cases and designs.

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Design and development of a heat engine with SMA actuator as a proof of concept prototype of a propulsion device for an in-liquid micro robot

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INTRODUCTION

Recently, there are various micro manipulation technology and used in various applications. However, they still need too many energy and space compared with targeted micro objects themselves. It is also difficult to manipulate multiple objects simultaneously and individually. If we realize self-propelled micro robot which harvests energy in a liquid, we can contribute to realize the micro robots which move simultaneously and individually. If we can develop micro robots whose sizes are less than 1mm^3 , we can move them inside blood vessels. We can expand a possibility that those micro robots become useful tools for bio-medical applications e.g. drug delivery and in-body micro surgery. However there are two main problems for realizing the micro robots. First one is “sticky problem”. If objects become less than 1mm^3 , it is difficult for them to move freely in the air because an electrostatic force, an interfacial force and a viscous drag force stick them. Second one is “energy source problem”. It is still very difficult to realize an effective battery whose size are less than 1mm^3 . Therefore, we suppose that those micro robots should harvest energy from their environment. If we examine the natural world, most of all microbes, for example water-flea and paramecium, live in a liquid. They are not only harvesting energy by eating food and/or absorbing sunlight, but also swimming freely in a liquid. This is because electrostatic and interfacial forces in a liquid become much less sticky than those in the air. That is why liquid is a suitable environment for the micro robot. However, there are only a few research activities for the self-propulsion micro robots which harvest energy in a liquid thus far. In previous research, we have analyzed unique in-liquid micro robot which propels by the comb actuator and harvests energy from the liquid by using the principle of the voltaic cell. However as

a result of the calculation by simple linear model, we confirmed that it cannot obtain a sufficient amount of energy to move the micro robot. [1] Therefore we have proposed the micro robot which moves by a heat engine with shape memory alloy (SMA) coil spring. Various heat engines have been developed. [2~5]

The purpose of this report is design and development of the proof-of-concept prototype of the heat engine less than 1cm^3 for understanding the required performance of the micro heat engine with 1mm^3 . We propose the mechanical model with 3 gears, 2 pulleys and the coil-shaped SMA and analyze in a steady state to formulate the output torque and generated force. We have selected one of the thinnest commercially available SMA actuator whose diameter is 0.05 mm. In several experiments, we have confirmed that the selected SMA actuator has much of stress hysteresis which generates much of non-linear errors from a coil-spring model. We have also evaluated an influence of a miniaturization of the engine to the force. We have discussed the future works of the heat engine to miniaturized less than 1mm^3 to apply to the in-liquid micro robot.

Motion analysis in the steady state of the heat engine

FIGURE.1 shows the mechanism of the heat engine that we have designed. Table 1, 2, 3, and 4 show the definitions parameters of this analysis. As listed in table 3, we assume the coil spring SMA is approximately linear spring for simplification.

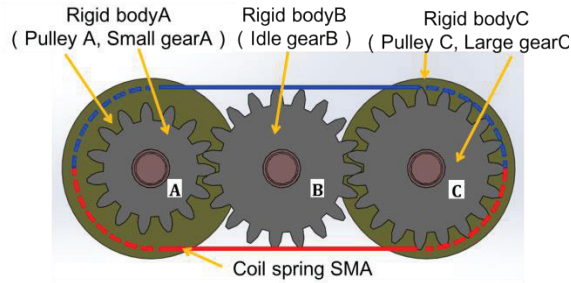


FIGURE 1. Model of heat engine

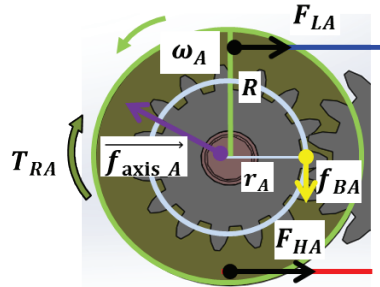


FIGURE 2. Forces and torques acted on A

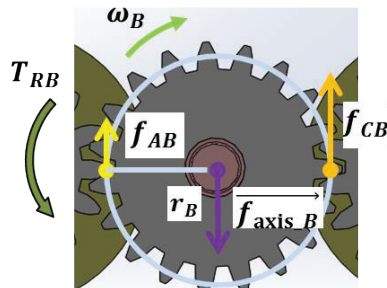


FIGURE 3. Forces and torques acted on B

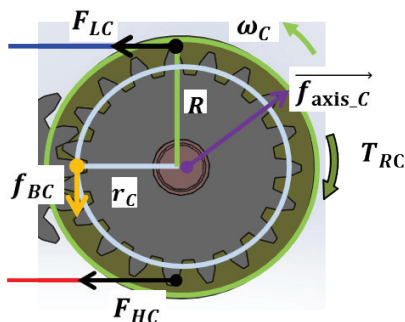


FIGURE 4. Forces and torques acted on C

TABLE 1 Dynamical model parameters

Symbol	Quantity
I_A, I_B, I_C	Moments of inertia of A, B, C
$\omega_A, \omega_B, \omega_C$	Angular velocities of A, B, C
T_{RA}, T_{RB}, T_{RC}	Torques acted on A, B, C from outer forces

T_{OA}, T_{OB}, T_{OC}	Output torques from A, B, C
F_{HA}, F_{HC}	Forces acted on gear A and C from SMA in high temperature region
F_{LA}, F_{LC}	Forces acted on gear A and C from SMA in low temperature region
F_H, F_C	Absolute values of F_{HA} (F_{HC}) and F_{LA} (F_{LC})
f_{BA}, f_{AB}	Force acted on A(B) from B(A)
f_{CB}, f_{BC}	Force acted on C(B) from B(C)
$\vec{f}_{axisA}, \vec{f}_{axisB}, \vec{f}_{axisC}$	Binding forces acted on A, B, and C from their own axes

TABLE 2 Design geometric parameters

Symbol	Quantity
r_A, r_B, r_C	Pitch radius of gear A, B, C
R	Radius of pulleys of A and C
H	Contact length of the SMA coil spring to each pulley

Modeling of the coil spring SMA

When performing the motion analysis, the approximate linear spring coiled SMA, as defined the following parameters.

TABLE 3 Parameters of the coil spring SMA

Symbol	Quantity
d_s	Wire diameter
D_s	Diameter of coils
n_0	Number of coils per a unit of length (linear coil density) at natural length
L_0	Natural length
ΔL_0	Initial displacement without temperature difference
N	Total number of coils

TABLE 4 Parameters of the coil spring SMA in low temperature

Symbol	Quantity
G_L	Shear modulus of rigidity
N_L	Number of coils
n_L	Number of coils per a unit of length (line density of coil)
e_L	Length of a coil at natural length
k_L	Spring constant of a part of SMA coil spring which is not contact with pulley
N_L'	Number of coils which are

	not contact with pulleys
y_L	Length of SMA inside Cold region

TABLE 5 Parameters of the coil spring SMA in high temperature

Symbol	Quantity
G_H	Shear modulus of rigidity
N_H	Number of coils
n_H	Number of coils per a unit of length
e_H	Length of a coil at natural length
k_H	Spring constant of a part of the coil spring which is not contact with pulleys
N_H'	Number of coils which are not contact with pulley
y_H	Length of SMA inside Hot region

We got the equations of motion (1)~(3) about rigid body A~C.

$$I_A \dot{\omega}_A = R(F_{HA} - F_{LA}) + r_A f_{BA} + T_{RA} \quad (1)$$

$$I_B \dot{\omega}_B = r_B f_{AB} + r_B f_{CB} + T_{RB} \quad (2)$$

$$I_C \dot{\omega}_C = R(F_{HC} - F_{LC}) r_C f_{BC} + T_{RC} \quad (3)$$

Steady state condition of the heat engine is represented as,

$$\dot{\omega}_A = \dot{\omega}_B = \dot{\omega}_C = 0 \quad (4)$$

Generated force of the SMA in high temperature and low temperature is (5), (6) from Hooke's law.

$$F_H = k_H(y_H - e_H \cdot N_H') \quad (5)$$

$$F_L = k_L(y_L - e_L \cdot N_L') \quad (6)$$

Here we add the following condition (7) ~ (9). First, (7) is the law of the conservation of the number of coils.

$$N = N_H + N_C \quad (7)$$

(8) is the condition for rolling contact. In other words, it is a condition that peripheral speed of the gear A and gear B is equal.

$$r_A \omega_A = r_C \omega_C \quad (8)$$

(9) is necessary condition when motion of the heat engine becomes uniform circular motion in its steady state. That means that interchanges of number of the turns in the two regions are balanced.

$$R \omega_C \cdot n_H = R \omega_A \cdot n_L \quad (9)$$

We substituted (5) ~ (9) for (1) ~ (3). And we formulated generated force and output torque. (10), (11) show generated force ΔF and output torque T_{OA} .

$$\Delta F = \kappa \left\{ \delta \varepsilon + 1 - \frac{1+i}{2} \cdot (-\zeta \delta + \frac{1}{\zeta} - \delta + 1) \right\} \quad (10)$$

$$T_{OA} = \kappa \cdot \frac{1+i}{2} \cdot \delta \cdot \frac{-1}{\zeta} \cdot (\zeta - 1)(\zeta^2 + \eta \zeta - \frac{1}{\delta}) \quad (11)$$

TABLE 6 Non-dimensional parameters

Symbol	Quantity
K	Function of the pulley radius and physical properties of the SMA
δ	Shear modulus ratio of high and low temperature of the SMA
ε	Distance between the coils ratio of high and low temperature of the SMA
ζ	Pitch radius ratio of the gear
i	The ratio of natural length and initial displacement

By solving (11) when the output torque T_{OA} becomes maximum value, we can formulate optimum ζ as the solution of cubic function as,

$$\zeta_1 = \sqrt[3]{-\xi + \sqrt{\xi^2 + \nu^3}} + \sqrt[3]{-\xi - \sqrt{\xi^2 + \nu^3}} - \frac{a}{3} \quad (12)$$

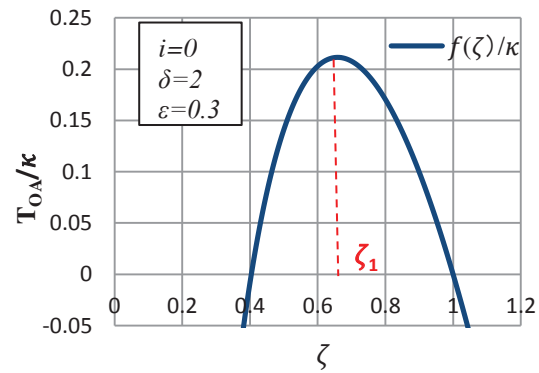


FIGURE 5. Plot of non-dimentional toeque vs ratio of pitch radius

FIGURE.5 is plot of ζ vs. $f(\zeta)/\kappa$ at $i=0$, $\delta=2$, and $\varepsilon=0.3$. We see that ζ should be larger than 0.4 and smaller than 1 to make the heat engine rotate continuously

Effect on the generated torque by the T_{OA} parameter change

FIGURE.6 shows that the larger δ and ε contribute larger maximum value of T_{OA} . We should choose SMA which has larger δ and ε to get larger output torque. FIGURE 7 shows that the larger i contributes larger maximum value of

T_{OA} . i is a design parameter. We should lengthen SMA as much as possible within range of elastic deformation to get larger T_{OA} .

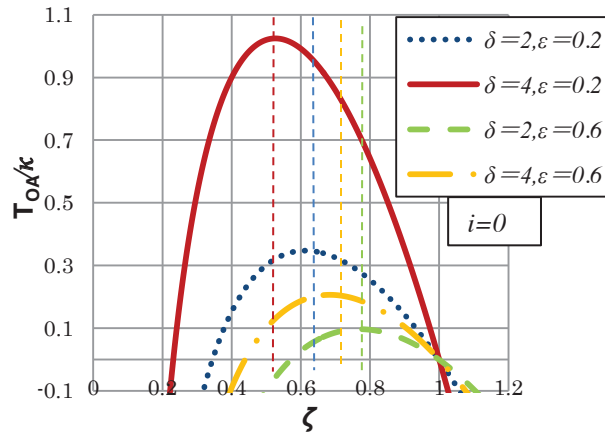


FIGURE 6. Plots of T_{OA}/k vs. ζ at 4 sets of δ and ε

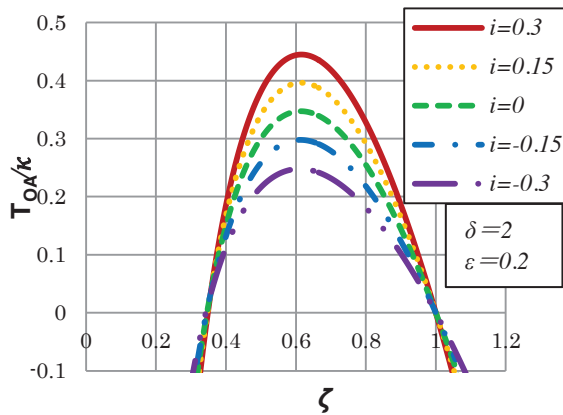


FIGURE 7. Plots of $f(\zeta)$ vs. ζ with various i

Generative force measurement experiment of SMA

We selected (Toki corporation BMX150) SMA and experimented with relationship between the displacement and the generated force. [6]

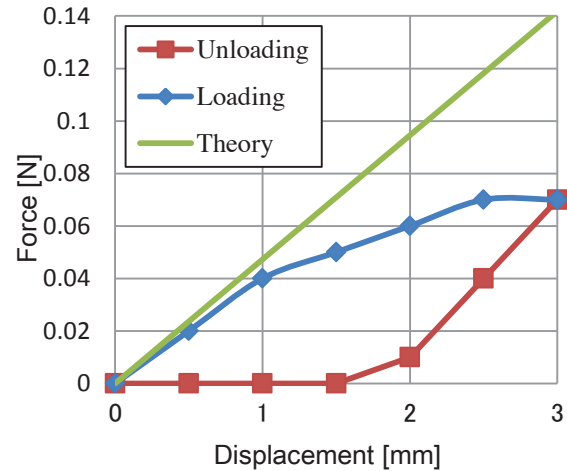


FIGURE 8. Plot of displacement vs. force

We checked that there is hysteresis characteristic [7~8] although we assume that the coil-shaped SMA performs as a linear spring in our analysis as represented in (5) and (6). To investigate the influence of the hysteresis characteristic to the analysis, we fabricated the proof of concept prototype of the heat engine as next section.

2D-structure heat engine

FIGURE.9 shows the exploded view of two-dimensional (2D) structure heat engine that we have designed. In this design, the heat engine is composed of three gears, two pulleys, and the SMA coil spring. FIGURE10 shows the prototype model. The size is about $60 \times 30 \times 3$ [mm³].

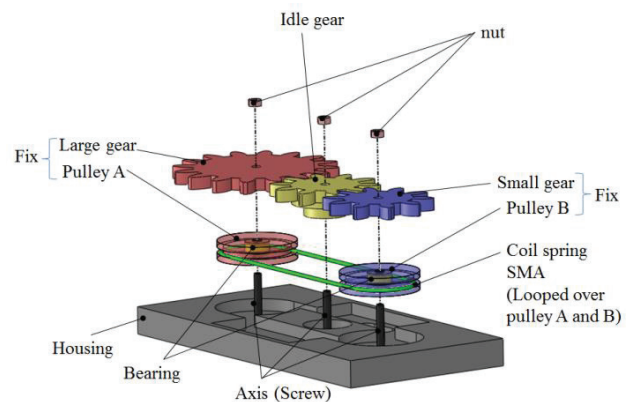


FIGURE 9. Exploded view (Two-dimensional)

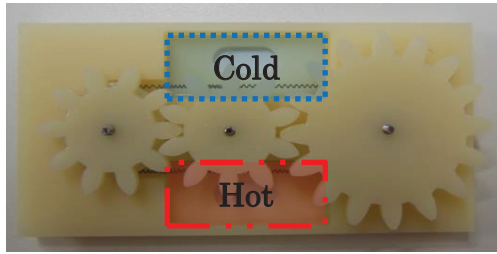


FIGURE 10. Prototype ongoing development (Two-dimensional)

We confirmed that this structure is feasible as micro engine of the in-liquid micro robot because we can minimize all components of this 2D-structure heat engine using micro processing method such as MEMS process, wire electric discharge machine and laser beam lithography. We plan to assemble the minimized components into the heat engine used by wet tweezers based on capillary force. [9] However, we also found that volume of the pool is not enough to heat and/or cool the SMA coil spring. That is the reason why we improve the design of the heat engine in next section.

3D-structure heat engine

We designed three-dimensional (3D) structure heat engine to get enough heating and cooling efficiency of the SMA. FIGURE 11 and 12 show the exploded view and photograph of the 3D-structure heat engine respectively. In this design, the heat engine is composed of three gears, and eight pulleys. The size is about 60×30×45[mm³]. Motion principle is the same as the 2D-structure one.

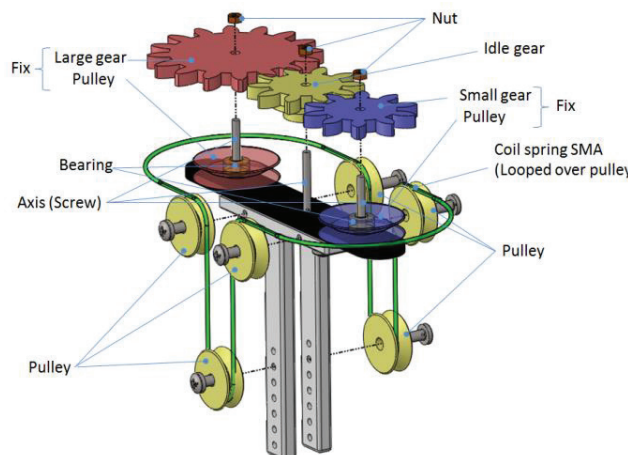


FIGURE 11. Exploded view of 3D-structure heat engine

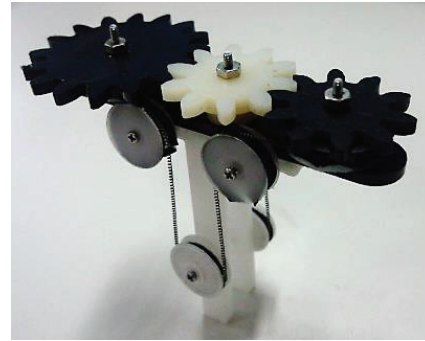


FIGURE 12. Photograph of 3D-structure heat engine

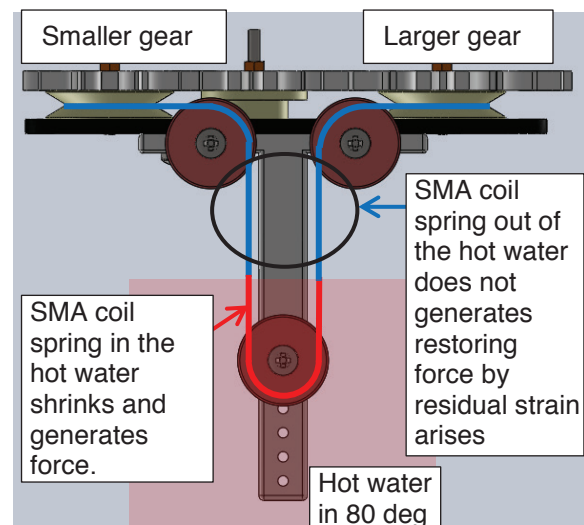


FIGURE 13. Residual strain of SMA coil spring out of the hot water

As shown in FIGURE 13, we found that this 3D-structured heat engine is still not enough to get continuous rotation because residual strain arises due to the SMA hysteresis in the region out of the hooter water.

Conclusion

We have described that the feasibility of the heat engine as the source of motion of the in-liquid micro robot with 1 mm³ volume. Then, we have formulated the generated force of heat engine ΔF , output torque T_{OA} by using linear model. We have finally formulated the ratio of the pitch circles of the 2 gears that give us maximum output power at its steady state. We have also fabricated 2 types of the heat engines; however we found that the heat engines were not able to get continuous rotation because of the SMA hysteresis.

Future works

We plan to improve the design of the heat engine considering the effect of the hysteresis and/or develop the SMA coil spring that is less affected by the hysteresis. We also plan to inspect validity of the analysis through measuring the angular velocity, torque and generative force. We will also develop the heating and cooling method of SMA in liquid to realize the in-liquid micro robot.

Acknowledgement

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SMART MATERIALS, STRUCTURES & NDT in AEROSPACE

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FREEFORM SURFACE RECONSTRUCTION FROM SECOND DERIVATIVE DATA

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OVERVIEW OF FREEFORM SURFACE METROLOGY

Recently demand for optical surfaces with freeform is increasing. They may provide possibility to improve the optical performance and reduce the number of optical elements in the optical system. While flat and spherical surface metrology is performed with commercialized full-aperture interferometer such a Twyman-Green and Fizeau type with an absolute reference metrology frame, aspheric or freeform surface metrology needs alternative method. They may have no symmetry and may have large departure from sphere. Figure 1 shows the overview of freeform surface metrology. CMM(Coordinate Measuring Machine) could be one alternative, but with limited spatial sampling and accuracy.

Null testing could be one possibility to cover insufficient dynamic range of full-aperture interferometer, however the use of null optics not only adds cost but also creates additional metrology problems and increase measurement uncertainty. The most problematic aspect of freeform surface testing with null-lenses is the loss of universality in the metrology tool. Because a new surface geometry will generally require a new null-lens.

Without null-lens, one alternative is the sub-aperture testing method that samples very small regions of the surface to determine the measurand concerned with surface profile over the entire surface and then combines them into a unified profile of the test surface. For the sub-aperture testing, the presence of additional mechanical and optical degree of freedom has

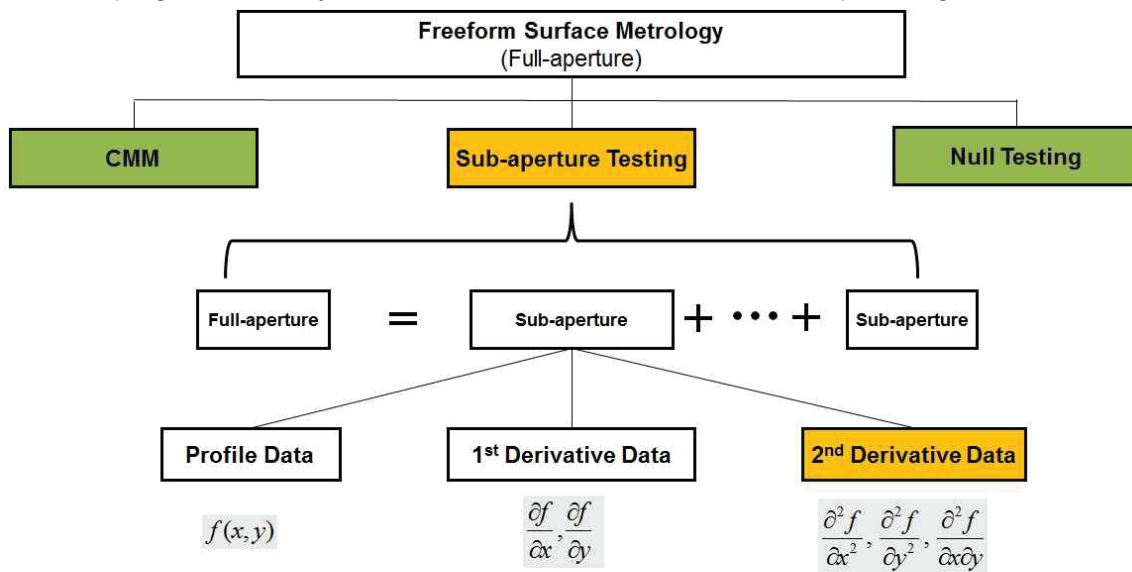


FIGURE 1. The overview of freeform surface metrology

led to unavoidable errors in mechanical alignment and motion control caused by temperature effects and non-orthogonality between each axis on the machine. These problems are related to the measurand obtained from the sub-aperture. The second derivative of sub-aperture profile is one of the promising measurands to reduce errors led in sub-aperture technique. The curvature, strongly correlated with the second derivative, is an intrinsic property[1] of the sub-aperture profile, which does not depend on the tilt motion of probe. Therefore we can reconstruct the shape of a profile $z(y)$ from measurements of its curvature $k(y)$. Glenn[2][3] first applied this curvature detection idea to the measurement of surface profile by measuring the local surface profile instead of by detecting the intensity variation. Schulz[4] and Elster[5] have shown that the reconstruction is indeed possible with an uncertainty that makes the method an attractive candidate for precision optical metrology. In cartesian coordinates the relation between a one-dimension profile $z(y)$ and its curvature $k(y)$ is given by the following non-linear differential equation[6].

$$\frac{d^2 z(y)}{dy^2} = k(y) \cdot \left[1 + \left(\frac{dz(y)}{dy} \right)^2 \right]^{\frac{3}{2}} \quad (1)$$

This shows the curvature $k(y)$ is strongly correlated with the second derivative, when the first derivative is zero, the curvature gets to be same as the second derivative. This is a form of the Frenet equations which describe arbitrary curves in space. Equ.1 may be interpreted in two ways: either given a profile $z(y)$, the curvature of a profile at any point can be calculated, or, when the curvature $k(y)$ is known, it can be used to reconstruct the profile $z(y)$ by integration.

Schulz and Elster restrict themselves to the measurement of one-dimensional surface profile $z(y)$ in a plane where z is the profile "height" and y the position along the scanning line. Unlike the profile, a surface $S(u,v)$ is not simply determined by its curvatures. (u, v denotes a pair of coordinates, for example cartesian coordinates x, y or polar coordinates r, θ). In the early 19th century one of the great accomplishment of C.F.Gauß[5] was to show that a surface is uniquely determined, up to its orientation in

space, by the coefficients of the first and second fundamental forms.

In this paper, we have undertaken computer simulations to orient ourselves to the possibility of two-dimensional surface reconstruction and the resulting accuracy of surface reconstruction using Frenet equations. Two curvature $k(x)$ and $k(y)$ are used along two orthogonal direction as followings.

$$k(x) = \frac{z''(x)}{[1+z'(x)]^{3/2}} \quad (2)$$

$$k(y) = \frac{z''(y)}{[1+z'(y)]^{3/2}} \quad (3)$$

When whole full-aperture surface consists of $N \times M$ sub-aperture, two curvature matrix maps can be set as followings

$$K_x = \begin{bmatrix} k_{11}^x & \dots & k_{1m}^x \\ \vdots & k_{ij}^x & \vdots \\ k_{n1}^x & \dots & k_{nm}^x \end{bmatrix}, K_y = \begin{bmatrix} k_{11}^y & \dots & k_{1m}^y \\ \vdots & k_{ij}^y & \vdots \\ k_{n1}^y & \dots & k_{nm}^y \end{bmatrix} \quad (4)$$

Where ij is index number of measurement points, the order of sub-aperture positions. The exact solutions of Frenet Equation is widely known[7] along the one axis. Two slope matrix maps can be described as Equ.5.

$$S_x = \begin{bmatrix} s_{11}^x & \dots & s_{1m}^x \\ \vdots & s_{ij}^x & \vdots \\ s_{n1}^x & \dots & s_{nm}^x \end{bmatrix}, S_y = \begin{bmatrix} s_{11}^y & \dots & s_{1m}^y \\ \vdots & s_{ij}^y & \vdots \\ s_{n1}^y & \dots & s_{nm}^y \end{bmatrix} \quad (5)$$

Where s_{ij}^x and s_{ij}^y are calculated from the solution of Frenet's Equation. [7]

$$s_{ij}^x = \frac{\varphi_{ij}^x(x) + \alpha}{\sqrt{1 - (\varphi_{ij}^x(x) + \alpha)^2}}, \quad \varphi_{ij}^x = \int_{x_0}^x k_{ij}^x(\theta) d\theta \quad (6)$$

$$s_{ij}^y = \frac{\varphi_{ij}^y(y) + \alpha}{\sqrt{1 - (\varphi_{ij}^y(y) + \alpha)^2}}, \quad \varphi_{ij}^y = \int_{y_0}^y k_{ij}^y(\theta) d\theta \quad (7)$$

Where φ denotes the integrated curvature κ . From the two slope maps, we can determine the reconstructed surface profile by using integration method, such as simpson integration method or

southwell's integration method. Finally as shown in Equ.8 reconstructed surface maps are calculated.

$$P = \begin{bmatrix} P_{11} & \dots & P_{1m} \\ \vdots & P_{ij} & \vdots \\ P_{n1} & \dots & P_{nm} \end{bmatrix} \quad (8)$$

RESULTS

At first, the test part for the simulations was a rotationally symmetric quadric surface which is described by the following well known Equ.9

$$Z(r) = \frac{Cr^2}{1 + \sqrt{1 - (1 + K)C^2r^2}} + \sum_{i=1}^{30} A_i r^i \quad (9)$$

Where $r^2 = x^2 + y^2$, $C = 1/\text{Radius of curvature}$

$K = -(\text{eccentricity of conic surface})^2$: Conic constant

The conic constant K was set to -0.998286 which makes the surface a ellipsoid of rotation. Radius of curvature is 36,000mm and the diameter of test part is 8,316mm with 18mm lateral sampling resolution. Figure 2 shows the process of surface reconstruction, which follows the process from Equ.2 to Equ.8. The results of simulation turns out to be 0.065nm PV and 0.013nm RMS reconstruction error.

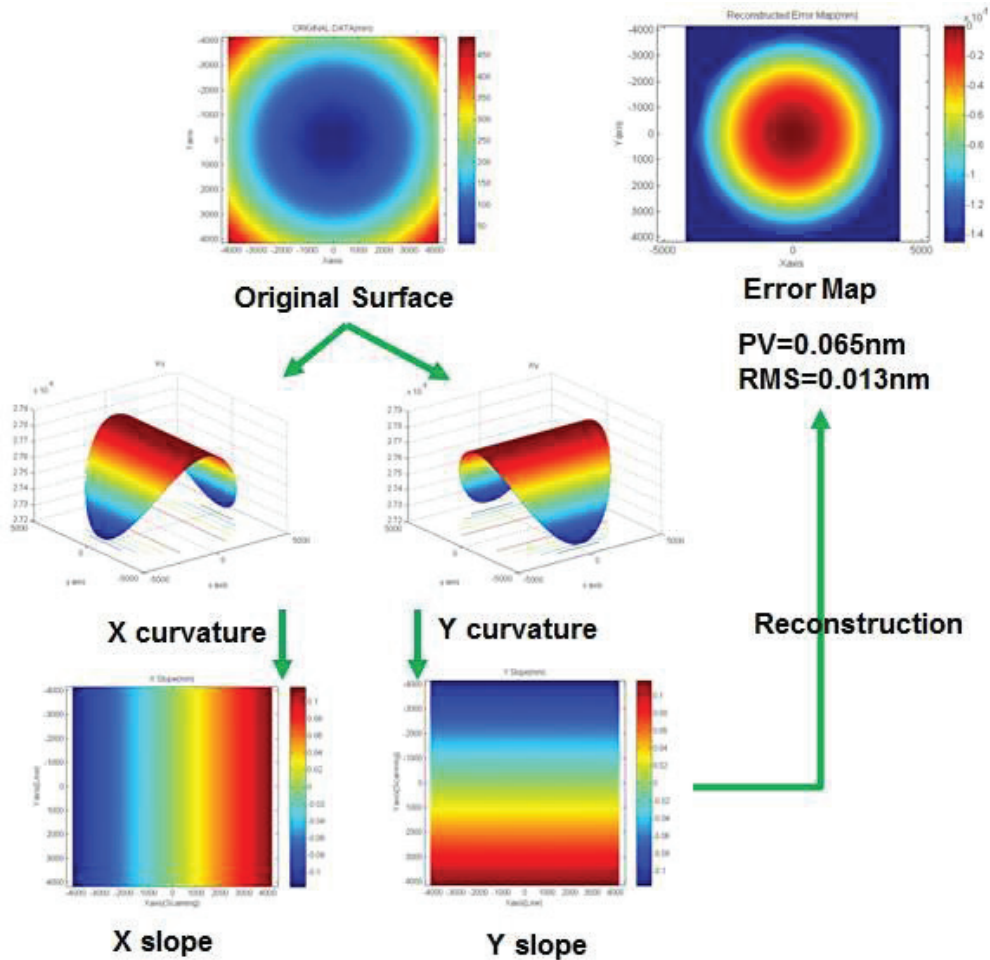


FIGURE 2. The process of surface reconstruction:

POSITION ERROR DIAGNOSIS

For the position error diagnosis, we assume that the position errors of stage are radomly distributed around ideal position as shown in Figure 3. δ_x and δ_y are maximum error imposed on measurement postion to two direction with 2nm, 20nm, 200nm, 2 μ m, 20 μ m, 200 μ m, 2mm respectively.

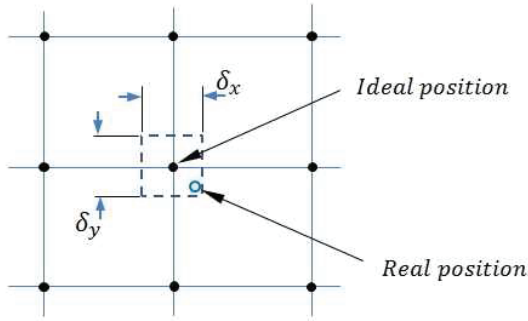


FIGURE 3. Position random error boundary

As a result of position error diagnosis, when there is stage random error with up to maximum 2mm, only 30nm PV, 8.7nm RMS reconstruction error was caused by position error. Figure 4 shows the simulation results and Figure 5 is radom error maps. The second derivative method is proved to be robust against position error.

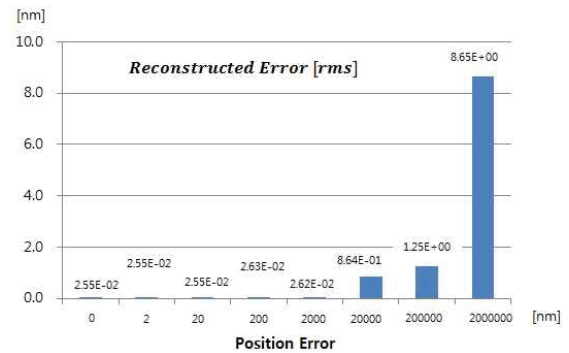


FIGURE 4. Surface reconstruction error caused by stage position error(2nm,20nm,200nm,2 μ m, 20 μ m, 200 μ m,2mm)

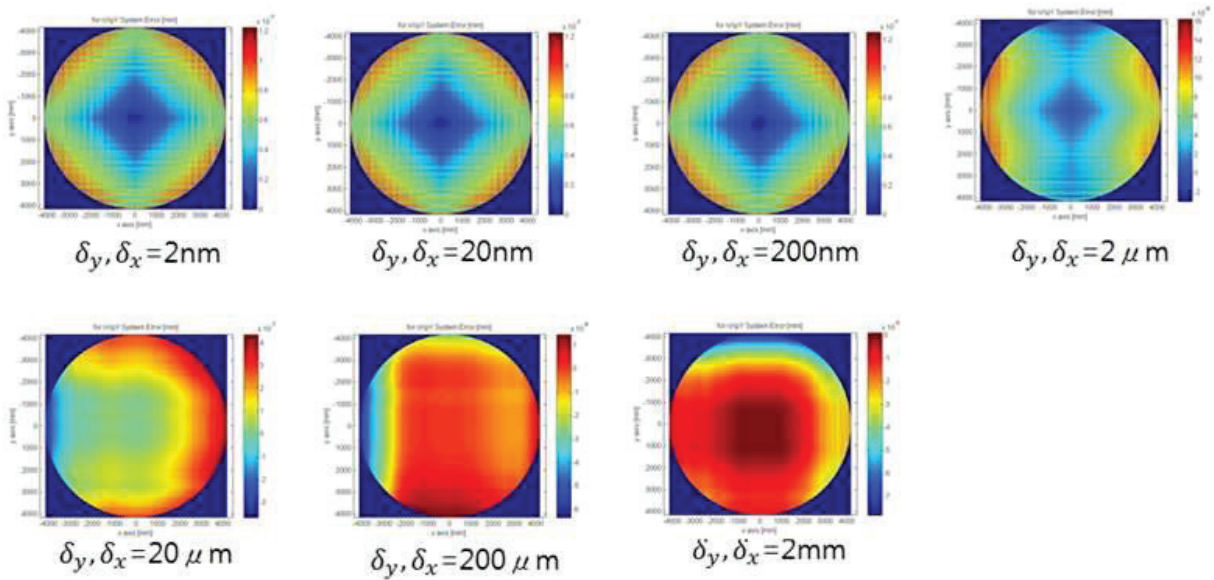


FIGURE 5. Reconstructed surface error caused by stage position error

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The 5-degree of freedom setting error calibration algorithm for end-point device or rotor to manufacture freeform surfaces

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INSTRUCTION

Various mechanical movements are a combination of linear movement and rotational movement. Rotational movement is applied to many different types of spindles, such as turning machines, positioning motors, engine shafts, flywheel shafts and others.

For their mechanical performance the stable movement of the motor is important, so that their additional 5-degree or freedom error motions even though an ideal rotor has only one degree of freedom rotational movement. To measure and evaluate the error motions of a rotor, several displacement signals, which are called runout, are acquired by several displacement sensors on the surface of the rotor or an artifact attached to the rotor.

Since the runouts consist of not only the error motions but also two other error components of artifact setting error and artifact roundness profile. In case of milting or turning machine the 5 degree of freedom setting error of the attached device such as a diamond turning tip is very critical for the machining accuracy.

For runout measurement method and the method of setting error separation from the runout data are well introduced in "ISO 230-7 Geometric accuracy of axes of rotation". An example of a standard five sensor system is shown in Figure 1 [1].

However, in the standard, the setting errors so called fundamental errors are deleted out in several different steps and the 5-degree of freedom values of the setting error cannot be calculated nor used for re-setting the tool position. Therefore, in this paper, a new setting error data processing algorithm is presented and evaluated by a simulation test.

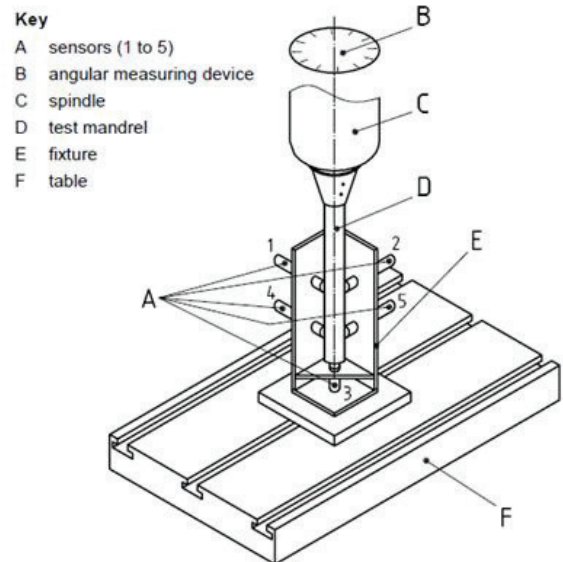


FIGURE 1. A standard five-sensor measurement system (ISO 230-7).

STANDARD DATA MODEL AND PROCESSING METHOD OF THE FUNDAMENTAL ERROR

As mentioned above, in the relevant ISO, the runout data is modeled to consist of three major components: error motions, artifact roundness profile, and fundamental error. This paper is about calculating this fundamental error. The fundamental error is caused by miss-setting the artifact onto the rotating center. If the geometric center of the artifact is ideally set onto the rotating center then there is no fundamental error but in real situation, there is generally significant amount of five degree of artifact setting error.

The runout data is usually expressed in a polar plot. The example is shown as Figure 2 in the relevant ISO. Here, the LSC (least squares center) is different from the PC (polar center).

The radial error motion is peak to valley value in terms of the LSC. In the figure, a is the peak to valley of asynchronous radial error motion which is random components and b is the peak to valley of synchronous radial error motion which is mean components of data acquired for several rotations. The LSC represents mis-setting of sensors and the radius represents fundamental error.

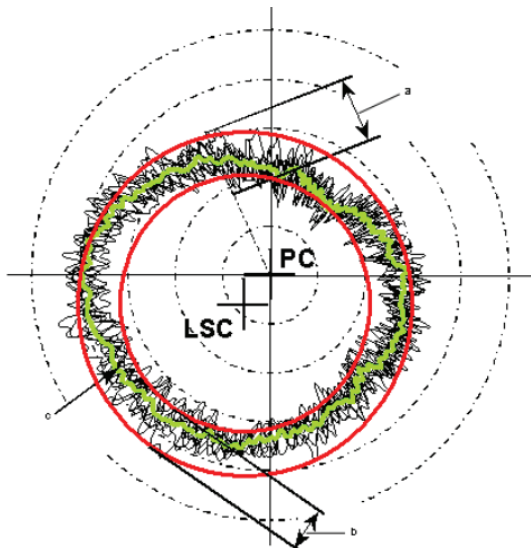


FIGURE 2. Error motion polar plot (ISO 230-7).

The runout data consisting of only two degree of radial error motions and fundamental error after a roundness separation process usually by reversal method or multi-probe method is defined as the model below.

$$r(\theta) = r_0 + \Delta X(\theta)\cos\theta + \Delta Y(\theta)\sin\theta \quad (1)$$

where θ is the angular position of the spindle, $r(\theta)$ is the radial error motion at angular position θ , $\Delta X(\theta)$ is the output of the displacement sensor oriented with the X axis, $\Delta Y(\theta)$ is the output of the displacement sensor oriented with the Y axis, r_0 is the value of the radius set by the alignment of the displacement sensors and the test artifact.

In this formula, the value of the radius set r_0 has the fundamental error caused by mis-setting of the artifact. However, this formula doesn't consider the fundamental error as a five degree freedom error. Even though, the fundamental error is generally very small that the formula is accurate enough as an approximate one. When more precise measurement is required or the artifact mis-setting is measured, a more precise formula must be derived. In this paper, a more

precise formula is derived and a new algorithm to separate the runout data into error motions and 5 degree of freedom fundamental error is presented.

THE RUNOUT DATA PROCESS ALGORITHM

To calculate the fundamental error, the runout data must be analyzed first. Each runout data acquired by each displacement sensor contains five degree of freedom error motion, fundamental error due to artifact mis-setting, roundness profile error of the artifact roundness form profile, and sensor setting error. Each error components must be separated serially. In this paper, a runout signal process algorithm is proposed as below.

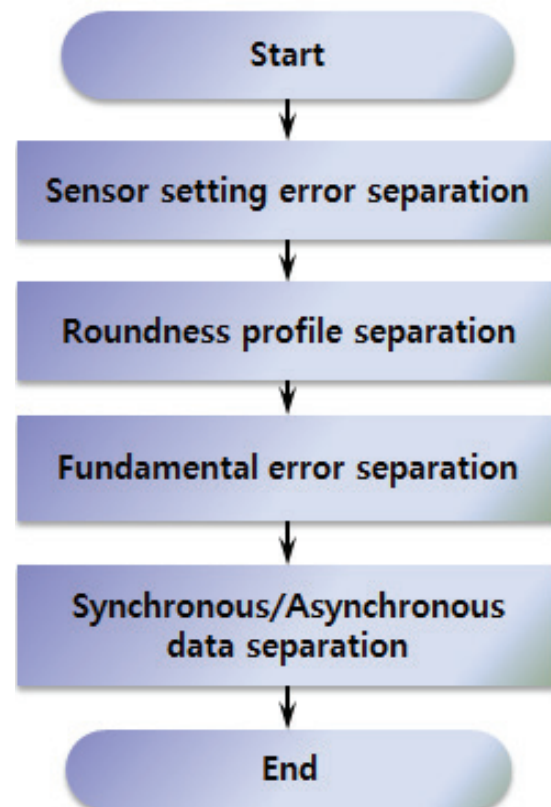


FIGURE 3. Data processing algorithm

First, the sensor setting error separated by setting the average of displacement values as the zero point in each runout data sets, as explained in detail in Marsh's book [2] or calculated as LSC as illustrated in Figure 2 in the ISO [1]. Second, roundness profile data is separated out by reversal method, multi-probe method, or multi-step method. In this paper, multi-probe method was used for its no need of

multi measurement process which requires resetting of the artifact and sensors. Third, fundamental error precisely is separated and the fundamental error can be used to reset the artifact or the end-tool attached on the rotor. This algorithm of third step is the main attribution of this paper and explained in the next two sections. Final step is synchronous and asynchronous error separation.

THE NEW FUNDAMENTAL ERROR MODEL

In this paper, the geometrically average of the artifact is newly defined as the artifact axis. The actual fundamental error has five degree of freedom components from the rotational axis to the artifact axis. So, the five degree of freedom fundamental error of artifact axis must be included in the runout data formula to calculate the fundamental error. The measurement is done with five or more sensors to include tilt error motion measurement, which need two layers of sensors arranged in the same height as shown in the Figure 1. The new runout data considering a five degree of freedom fundamental error is derived as two formulas as below.

$$r_u(\theta) = r_{u,0}(\theta_u + \phi_u) + \Delta X_u(\theta)\cos\theta + \Delta Y_u(\theta)\sin\theta \quad (2)$$

$$r_d(\theta) = r_{d,0}(\theta_d + \phi_d) + \Delta X_d(\theta)\cos\theta + \Delta Y_d(\theta)\sin\theta \quad (3)$$

where the all parameters are the same with them the equation (1) and the subscribe u and d represents upper and down layers of the measurement system, respectively. And ϕ is the initial angle by the alignment of the and the test artifact, which is newly added.

The two size of the two least squares fitted radius and two initial angle values altogether due to four degree of freedom fundamental error except one translational error along the rotational axis which is trivial and cannot be measured and evaluated with the sensor arrangement condition.

The acquired runout data with two layers of measuring units can be illustrated as Figure 4. The two circle shaped scattered data sets represents the runout data sets and the bottom circle which is projected represents the geometrical center of the rotor. With the points and lines the radial, tilt, axial error motions can be measured and expressed graphically.

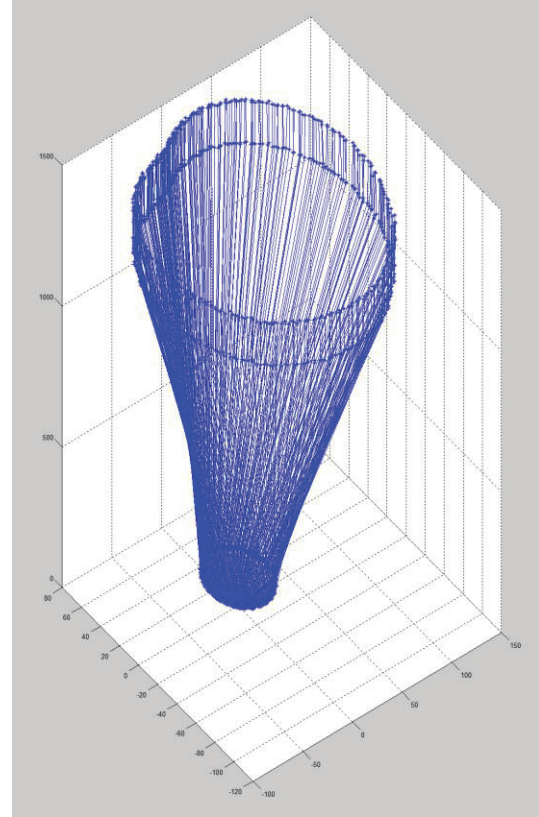


FIGURE 4. Runout data plotting example

Here, the measured signals runouts are modeled as below

$$s_{x,u}(\theta) = r_u \cos(\theta + \phi_u) + x_u(\theta) \quad (4)$$

$$s_{y,u}(\theta) = r_u \sin(\theta + \phi_u) + y_u(\theta) \quad (5)$$

$$s_{x,d}(\theta) = r_d \cos(\theta + \phi_d) + x_d(\theta) \quad (6)$$

$$s_{y,d}(\theta) = r_d \sin(\theta + \phi_d) + y_d(\theta) \quad (7)$$

where the s is runout signal which is free from roundness profile. r is radius size of the runout data sets. x_u and y_u are two degree of freedom radial error motions measured by the upper measuring unit system. x_d and y_d are measured in the down measuring unit.

THE FUNDAMENTAL ERROR CALCULATION ALGORITHM

The four degree of freedom fundamental errors cause the two radius r_u , r_d and two degree shifts ϕ_u , ϕ_d . The four values r_u , r_d , ϕ_u , ϕ_d are calculated as the mean values of each radius $r_u(\theta)$, $r_d(\theta)$, and relative angle $\phi_u(\theta)$, $\phi_d(\theta)$ of runout data, as modeled below.

$$r_u(\theta) = \sqrt{(s_{x,u}(\theta))^2 + (s_{y,u}(\theta))^2} \quad (8)$$

$$r_d(\theta) = \sqrt{(s_{x,d}(\theta))^2 + (s_{y,d}(\theta))^2} \quad (9)$$

$$\phi_u(\theta) = \tan^{-1} \left(\frac{s_{y,u}(\theta)}{s_{x,u}(\theta)} \right) \quad (10)$$

$$\phi_d(\theta) = \tan^{-1} \left(\frac{s_{y,d}(\theta)}{s_{x,d}(\theta)} \right) \quad (11)$$

The calculation accuracy was verified by simulation test that the runout data sets were generated with fundamental error and runout data sets were separated into error motions and fundamental error. The calculated fundamental error shows good accordance with the generated fundamental error for simulation. In Figure 5, the vector which is decided by the average value of $r_u(\theta)$, $r_d(\theta)$, and $\phi_u(\theta)$, $\phi_d(\theta)$ represents the four degree of freedom fundamental error.

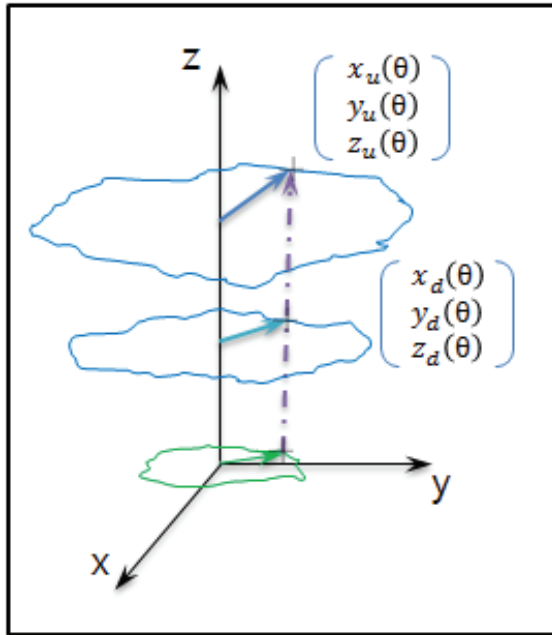


FIGURE 5. Fundamental data structure

CONCLUSIONS

In measurement of rotational error motions, a fundamental error calculation algorithm was proposed to re-set the artifact or the end-effector onto the rotor center. A new data model was derived, which is more precise than the conventional one in the ISO 230-7. Based on the

new data model, a fundamental error calculation algorithm was also presented. By the algorithm, precise calculation of fundamental error size and direction is possible so that the end-effector or the artifact can be re-set onto the rotor center. Precision re-setting of the end-effector leads to performance improvement and artifact re-setting leads to using more sensitive displacement sensors with narrow measuring range for more precise measurement.

FUTURE WORK

In the future work, the overall algorithm using all three signal process steps of least squares fitting for center position estimation, multi-probe method for roundness error separation, and the proposed fundamental calculation method will be implemented and evaluated. For more rigorous evaluation of the overall algorithm, a measurement system consisting of a zig and eight sensors with data processing software will be developed. An artifact of which roundness profile is pre-known will be used to evaluate the roundness data separation algorithm. A multi-degree of freedom stage system will be used to give a multi-degree of fundamental error then the fundamental data separation will also be evaluated with the operating value of the stage system.

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DESIGN OF HYDROSTATIC SYMMETRIC-PAD BEARINGS OF PRECISION GRINDING MACHINES

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INTRODUCTION

Precision grinding machines are frequently used in the production process of high quality functional parts such as molds for the replication of optical elements. In optical mold machining, a fine grinding process is frequently used as the final process to reach high geometry accuracy. Polishing is then used to achieve the finest surface finish. In other words, the precision grinding machines is an effective tool frequently adopted to produce parts with high geometry accuracy.

In practice, hydrostatic bearings are frequently used in precision grinding machines because of several advantages such as low straightness ripple, high damping, and low friction. These features make hydrostatic bearings very attractive in applications that require high surface finish, high freeform accuracy, and maintain good surface integrity.

The working principle of hydrostatic bearings is based on the mechanism of fluid film lubrication [1]. This mechanisms can be characterized by the complete separation of the conjugated surfaces of a kinematic pair, by means of a fluid film, which is pressurized by an external piece of equipment to create the load carrying capacity. The bearings system forms the fluid film as soon as the pump is turned on. Consequently, this lubrication mechanism does not require a relative motion between the conjugated surfaces.

Numerous analytical studies in the field of fluid film lubrication have dealt with power consumption issues of hydrostatic bearings with given constraints and design criteria [2]. These studies took power consumption as the most important criterion for bearing optimization. The main reason is that once the specified operational temperature is established, a relatively constant viscosity could be ensured with favorable effects both on the bearing and on the supply system. If the lubricant

temperature increases, then viscosity decreases and therefore the flow rate rises and the control of the pump performances might be lost.

In this study, it is of interest to optimize the design of the bevel-symmetric bearing as shown in Fig1 and Fig2. The design criterion is to minimize the power consumption of the bearing system with known stiffness requirement.

In this paper, the fundamental theory of fluid film lubrication is derived first. The effects of system parameters on pad performance are then presented. At the end, design procedures were proposed and an example was shown.

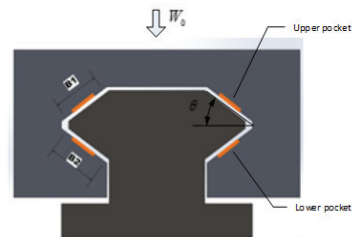


Fig1. Hydrostatic symmetric-pad bearing

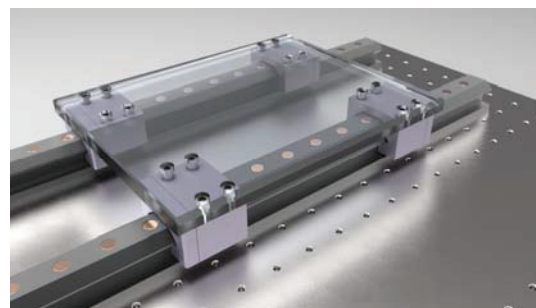


Fig2. A carriage plate system which is supported by four hydrostatic symmetric-pad bearings.

THE FLUID FILM LUBRICATION MODEL

The performance of a hydrostatic bearing may be affected by the flow pattern, heat dissipation, and the temperature distribution. The fluid film lubrication theory used to estimate the flow

pattern of the bearing were in the following paragraph. When the flow pattern is given, the heat dissipation of the bearing system can be roughly estimated. The analysis of bearing performances from the temperature distribution aspect are not included in this paper.

The fluid film lubrication theory is based on the classic Reynolds equation and Navier-Stokes equation with some boundary conditions [1][3][4]. Other assumptions used are basically extended from those used by Bassani and Piccigallo [5] and are listed below for completeness.

1. The dimensions of the fluid model is in the thin film regime: the thickness of the fluid film is small, compared to its size in the other directions.
2. No pressure gradient across the fluid film.
3. The flow is laminar: no turbulence nor vortex exists.
4. Nonslip boundary condition: on the surfaces bounding the fluid film, the velocity of the lubricant coincides with the velocity of the surfaces.
5. The pressure distribution contributed by the hydrodynamic effect is neglected.
6. The body forces are negligible compared to the viscous forces.
7. The inertia effect is small and can be neglected.
8. The ambient pressure is assumed to be zero.
9. The recesses size of the bearing are equal.
10. The bearing is not tilted and its surfaces are perfectly smooth.
11. The fluid is Newtonian fluid.

In this study, a rectangular pad, which has been rounded along four corners shown in Fig.3 is used.

SINGLE PAD BEARINGS

In this study, four performance index were taken into account: load capacity W_z , static stiffness k , supply flow rate Q , and power consumption of the system H_p .

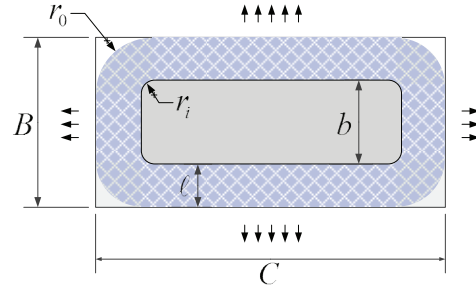


Fig.3. Rectangular pad

The load capacity of the rectangular pad was estimated by the integration of the pressure distribution of the fluid film. The pressure distribution of the fluid film was divided into two parts, a circular region and a finite length rectangular region, and was solved individually using the two-dimensional Reynolds equation [5]. The load capacity of the rectangular pad bearing can be presented as [1][5]

$$W_z = p_r A_e \quad (1)$$

Where p_r is the recess pressure, A_e is the effective area of the rectangular pad and is determine by the following equation:

$$A_e = BC - B\ell \left(1 + \frac{C}{B}\right) - 4r_i(r_i + \ell) + \frac{\pi((r_i + \ell)^2 - r_i^2)}{2\ln(1 + \ell/r_i)} \quad (2)$$

Where B, C, ℓ and r_i are the dimensions shown in Fig.3.

The stiffness k is generally defined as the ratio of a small change of the applied load to the consequent displacement along the load direction. When restrictors with constant hydraulic resistance are used as compensating devices (e.g. capillary tubes), the static stiffness of the single pad bearing can be described as

$$k = -\frac{dW_z}{dh} = \frac{3W_z}{h} \left(1 - \frac{p_r}{p_s}\right) \quad (3)$$

In other words, the stiffness is proportional to the inverse of the film thickness, $k \propto h^{-1}$.

The lubricant flow rate can be determined by the recess pressure and the hydraulic resistance of lands of the pad:

$$Q = \frac{P_r}{R_b} \quad (4)$$

Therefore, the hydraulic resistance R_b is proportional to the inverse of the third power of the film thickness h^{-3} as shown in the following equation:

$$R_b = \frac{6\eta_0}{h^3} \left(\frac{\pi}{\ln(1 + \ell/r_i)} + \frac{(C + B - 4(\ell + r_i))}{\ell} \right)^{-1} \quad (5)$$

The major power consumption of the system is the sum of the pumping power H_p and the friction power H_f . The friction power is not significant when the surface velocity is small, and it is often neglected in the precision engineering regime. The pumping power is the power need to push lubricant into the bearing and the power be dissipated, because of viscous friction, mainly in the compensating restrictor and the bearing lands.

The pumping power can be described as

$$H_p = p_s Q \quad (6)$$

where p_s is the pressure of the lubricant supported by the pumping device. The power consumption can also be expressed as a function of the load capacity:

$$H_p = \frac{h^3 W_z^2}{6\beta\eta_0 A_p^2} H_p^* \quad (7)$$

where H_p^* is the dimensionless pumping power [4].

The effects of the recess ratio, $\lambda (=b/B)$ and the length/width ratio $(=C/B)$ on the dimensionless power for the case studied is show in Fig 4. Therefore, a land ratio around 0.5 is preferred to minimize the power consumption of the bearing. It was also observed that a larger land ratio generally result in higher power consumption.

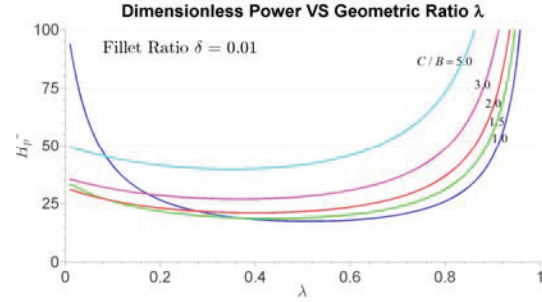


Fig.4. Dimensionless pumping power as function of land ratio and the length/width ratio

Once the power consumption is estimated, the temperature increase in the system can be further analyzed and hence the system performance can be more accurately evaluated. In most cases, the temperature will vary kept in a few degrees. If a large increase in temperature occurs, the viscosity of the lubricant cannot be considered as a constant, and the performance of the bearing system may be considerably affected.

BEARING PERFORMANCE

The performance of the symmetric-pad bearing were derived from the single pad equation described above.

The load capacity of a symmetric-pad bearing in the z direction were written as follows:

$$W_z = 2A_e \cos \theta (p_{r1} - p_{r2}) \quad (8)$$

A_e is the effective area of the pad 1 and pad 2. p_{r1} and p_{r2} are the pressure of the recess 1 and recess 2, respectively. And the bevel angle θ is the angle between the land and the X axis, as shown in Fig 5.

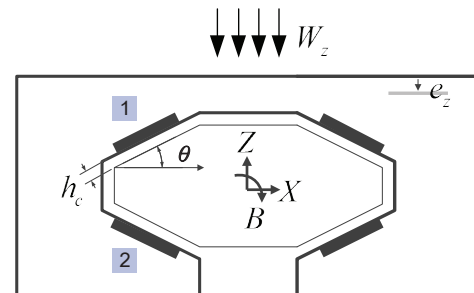


Fig.5. Bearing coordinates for a symmetric-pad bearing

The reference stiffness of the bearing is

$$k_z = \frac{6A_e \cos^2 \theta}{h_c} \left(\frac{P_{r1}}{(1-\varepsilon)} \left(1 - \frac{P_{r1}}{p_s} \right) + \frac{P_{r2}}{(1+\varepsilon)} \left(1 - \frac{P_{r2}}{p_s} \right) \right) \quad (9)$$

where h_c is the clearance of the bearing as shown in Fig.4. The eccentric ratio ε is the ratio of the eccentricity e_z to the projected clearance $h_c / \cos \theta$.

The total flow rate of the bearing is

$$Q = 2 \left(\frac{P_{r1}}{R_c} (1-\varepsilon)^3 + \frac{P_{r2}}{R_c} (1+\varepsilon)^3 \right) \quad (10)$$

where R_c is the hydraulic resistance of the pad at the zero eccentricity condition.

Since the hydraulic resistance of the restrictor is a constant, any change in the clearance between the land surfaces will consequently result in the change in the recess pressure.

The recess pressure can be estimated by

$$P_{r1} = P_s \frac{1}{1 + \frac{1-\beta}{\beta} (1-\varepsilon)^3} \quad (11)$$

$$P_{r2} = P_s \frac{1}{1 + \frac{1-\beta}{\beta} (1+\varepsilon)^3} \quad (12)$$

where β is the restrictor pressure which is the ratio of the recess pressure to the supply pressure, at the zero eccentricity condition (i.e. $\varepsilon = 0$):

$$\beta = p_c / p_s \quad (13)$$

From the above equations, the maximum stiffness at the zero eccentricity condition can be achieved when $\beta = 0.5$.

The dimensionless stiffness is defined as:

$$k^* = \frac{k_z}{A_e p_s / h_c} \quad (14)$$

Fig. 6 shows the effects of pressure factor and eccentricity ratio on dimensionless stiffness when the bevel angle is 0.

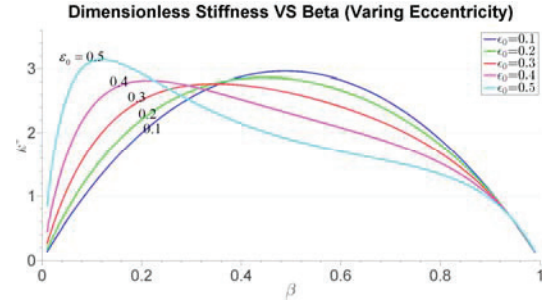


Fig.6. Stiffness as function of restrictor pressure factor and the eccentric ratio with $\theta = 0$

In general, a larger loading will result in the increase in eccentricity ratio. As shown in the figure, when a smaller pressure factor (< 0.2) is chosen a higher stiffness can be derived when a higher loading is applied. And when a larger pressure factor ($0.4 < \beta < 0.8$) is chosen a higher loading is applied will result in a lower stiffness.

DESIGN PROCEDURES

The procedures and strategies of hydrostatic symmetric-pad bearings design were proposed as the following:

1. The maximal allowable axial offset g_M , the maximum loading capacity W_M .
2. Limitations on system parameters should be identified. Possible limitations includes the minimal required stiffness k_m , the specification of the pad dimensions, including bevel angle θ , width of the pad B , length of the pad C .
3. The nominal clearance h_c of the bearing should be determined first. The nominal clearance is highly dependent on the machining capability of the alliance companies. The nominal clearance should also be twice larger than the maximal axial offset, $h_c \geq 2g_M$.
4. Refer to the relationship between dimensionless pumping power H^* and the recess ratio λ (Fig.4 as an example) to identify the proper value of recess ratio, which can lead to smaller power consumption.
5. Refer to the relationship between dimensionless stiffness k^* and the restrictor pressure factor β (Fig.6 as an example) to

identify the proper range of β at which the stiffness of the system is in condition.

6. With the determined dimensions of the pad *such as* B , C , and δ , and the recess ratio λ , the effective area A_e can be estimated by equation (2).
7. With the determined effective A_e , the restrictor pressure factor β , maximal eccentric ratio ε_M , and maximal load W_M , the supply pressure p_s can be estimated by equation (8).
8. With the effective A_e , the restrictor pressure factor β , the maximal eccentric ratio ε_M , the nominal clearance h_c , and the bevel angle θ , the stiffness of the bearing k_z can be estimated.
9. Make sure that the estimated bearing stiffness k_z is larger than the desired one. If the stiffness is smaller than the specification, *re-calculate* the supply pressure p_s by equation (14).
10. Estimate the operation performance of the bearing
11. Design the compensating devices for the bearing design

The design procedure is introduced for zero- and low-speed bearings. For high-speed bearings, modification is needed because of the inertial effect can no longer be neglected.

DESIGN EXAMPLE

If a hydrostatic symmetric-pad bearing is needed. The specification for the bearing is listed in Table 1.

Table 1 specification of the bearing

Stiffness	$\geq 3000 \text{ N}/\mu\text{m}$
Maximal allowable axial offset	$3 \mu\text{m}$
Nominal clearance	$10 \mu\text{m}$
Maximal load	7000 N
Bevel angle	30 degree
Pad size	$40 \times 80 \text{ mm}^2$

Other system parameters were estimated as following:

Maximal eccentricity ratio, $\varepsilon_M = 3/10 = 0.3$

Fillet ratio, $\delta = 0.01$

Viscosity of oil, $\eta_0 = 0.0413 \text{ PaSec}$

As shown in Fig.4, with a C/B of 2, a λ of 0.4, the power consumption is close to the minimum.

As shown in Fig.6, the restrictor pressure factor β should be in the range 0.4~0.8 to ensure a desired stiffness. In this case a small value of 0.4 for the restrictor pressure factor was used such that a larger margin for the working tolerances can be derived.

The effective area of the bearing can be estimated by equation (2), $A_e = 0.0018 \text{ m}^2$.

The supply pressure of the bearing can be estimated by equation (8), $p_s = 5.3 \text{ MPa}$.

The hydraulic resistance of a pad with zero eccentricity can be estimated by equation (5), $R_c = 3.756 \times 10^{13} \text{ N} \cdot \text{Sec}/\text{m}^5$.

The estimated hydraulic resistance of the restrictor for this symmetric-pad bearing is

$$R_s = R_c \frac{1-\beta}{\beta} = 5.634 \times 10^{13} (\text{N} \cdot \text{Sec}/\text{m}^5)$$

The stiffness of the bearing is $k_z = 1947.8 \text{ N}/\mu\text{m}$. The value is less than the required stiffness k_m . Therefore the supply pressure was modified by adopting equation (14). And k^* was determined using Fig.5, $k^* = 2.747$:

$$p_s = \frac{h_c k_z}{k^* A_e} = \frac{10^{-5} \times 3000 \times 10^6 / \cos^2(30)}{2.747 \times 0.0018} = 8.1 (\text{MPa}) \quad (14)$$

It was indicated that the supply pressure should be larger than 8.1 MPa to reach a desired stiffness.

The performances of the bearing with the designed parameters were plotted in Fig.6, Fig.7, Fig.8, and Fig.9.

CONCLUSIONS

A general approach for the design of a hydrostatic symmetric-pad bearing is worked out in this paper. Effects of two design parameters of the bearing, λ , β on the power and stiffness respectively are also demonstrated in this study for design consideration.

The design procedures to reach the minimum power consumption are validated for a

symmetric-pad bearings with proper specifications.

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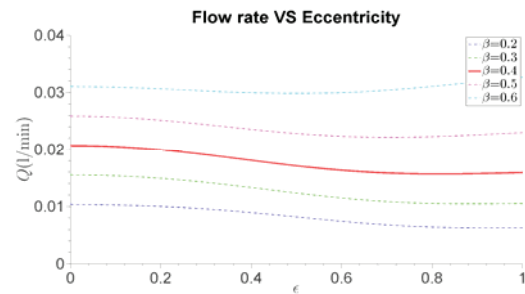


Fig.8. Flowrate versus applied load

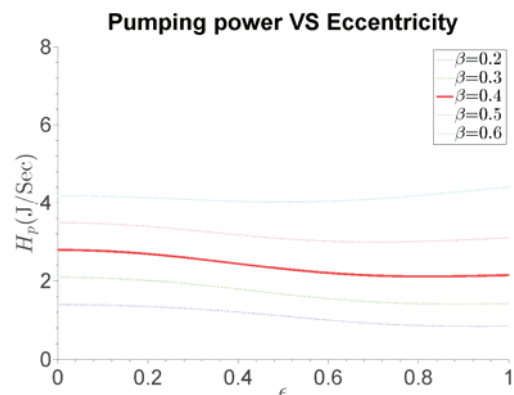


Fig.9. Power consumption versus applied load

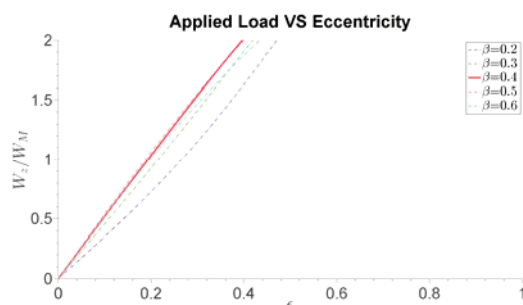


Fig.6. Applied load versus eccentricity

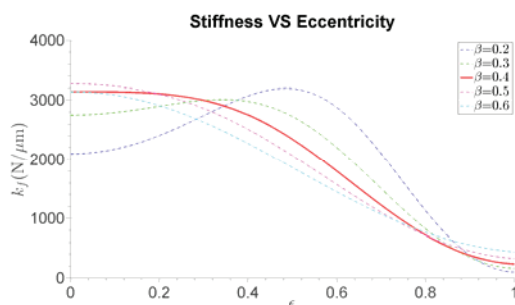


Fig.7. Bearing stiffness versus applied load

The 6-degree of freedom stitching method for the freeform surface

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INSTRUCTIONS

As ultra-precision machining systems improve, freeform surfaces such as Fresnel lens, micro structured surface, smooth surface are widely applied. As a consequence, the techniques to machine and measure freeform surface are also drawing attention [1]. To measure freeform surface, the required resolution is very high that leads to too small measuring range when using optical instruments. Therefore there are several stitching methods.

These methods are available only when the models or equations for the surface are known such as plane, sphere, and aspheric lens. For the freeform surface which cannot be represented with a single universal equation, conventional stitching methods have a critical limitation to be applied to [2-5].

In this research, a 6-degree of freedom stitching method for the freeform surface for freeform surface is proposed. The proposed stitching is a method which combines calculation for regression plane and 2D cross correlation for a local data.

Accuracy of the presented method is analyzed and evaluated. The method is applied to a freeform surface and the stitched results are also presented.

STITCHING ALGORITHM

Figure 1 illustrates six degree of freedom geometric error between two adjacent measured areas. Here, the six degree of errors consist of six error components: the linear positioning error Δ_x , horizontal straightness error Δ_y , vertical straightness error Δ_z , roll error Δ_r , pitch error Δ_p , yaw error Δ_y .

The stitching process has three steps. The first step is to calculate the vertical straightness error Δ_z , roll error Δ_r , and pitch error Δ_p , all together by the least squares method. The three calculated coefficients represent the three errors and used to stitch the two adjacent data sets.

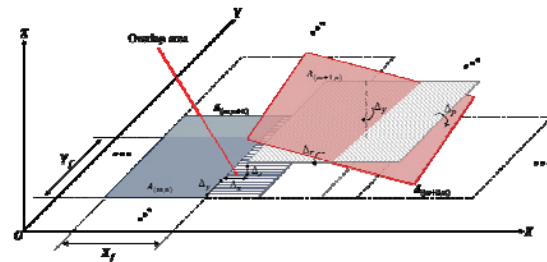


FIGURE 1. Two areas A1 and A2 are measure with a common area.

In the second step for calculating the yaw error Δ_y , as shown in Figure 3, two data sets are extracted from two different subareas from the overlap area of the two adjacent measured areas. From each of the two abstracted area, the three degree of freedom spatial delay amounts are calculated by cross-correlation. Then, the difference of the two delay amounts which are described as two vectors is used to estimate the yaw error Δ_y .

In the third step for calculating linear positioning Δ_x and horizontal straightness Δ_y , as shown in Figure 4 and 5, the spatial delay amount by cross-correlation is used again for estimated the amounts of the two error components.

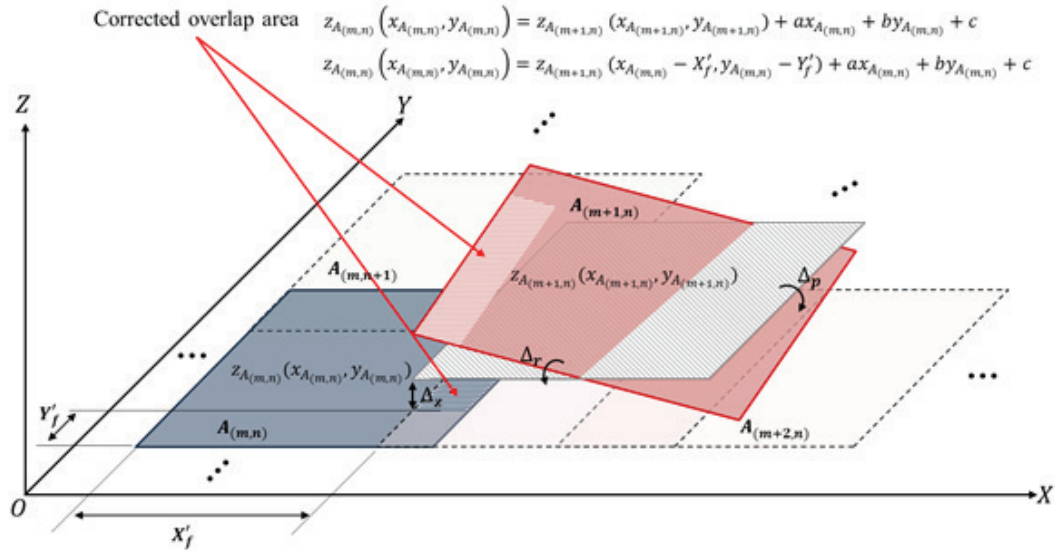


FIGURE 2. Adjacent measured data with vertical straightness, roll, and pitch

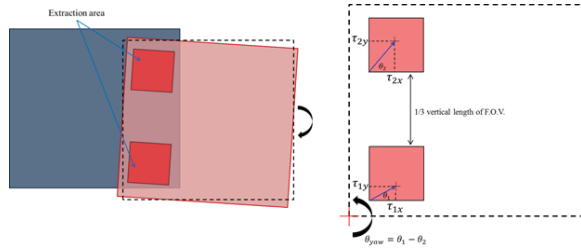


FIGURE 3. Extraction areas to correct yaw error

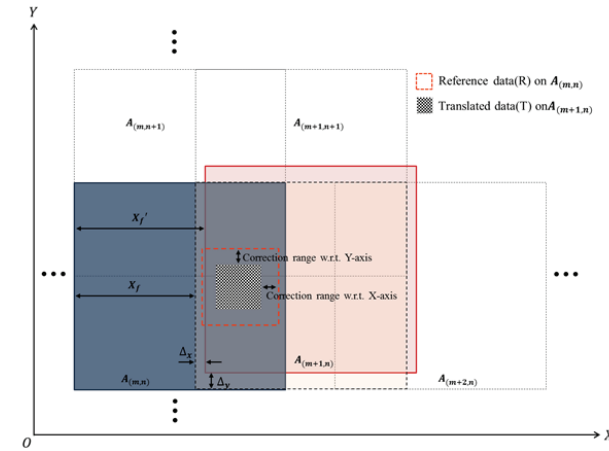


FIGURE 4. Extraction area to correct a linear positioning error and a horizontal straightness error

When the error values are calculated by the cross-correlation, the calculation accuracy depends on the position and size of the searching area. Especially when much there is much measuring noise or smooth surface is

measured, the accuracy of cross-correlation based error calculation decreases. Therefore, in this paper, the searching area is limited within the uncertainly level of the moving stage. For searching position choice, a random position is chosen and the correlation coefficients are calculated. Then, this process is repeated until the coefficients reach certain critical points, which is similar to RANSAC (Random Sample Consensus) algorithm.

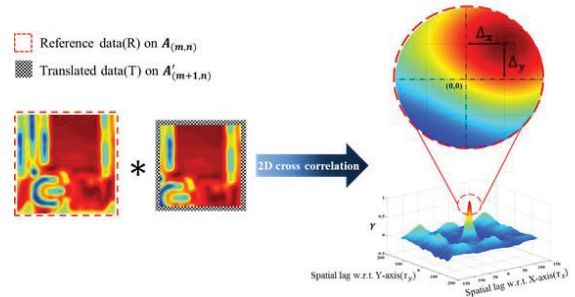


FIGURE 5. Estimating a linear positioning error and a horizontal straightness error using by cross correlation function and spatial delay

SIMULATION RESULTS AND ASSESSMENT OF THE ALGORITHM

For evaluation of the stitching method, a virtual measuring specimen was generated by measuring a Fresnel lens over 1,250 x 1,250 μm^2 with ten magnification and interpolating the data with 1 nm resolution, then finally random noise was added.

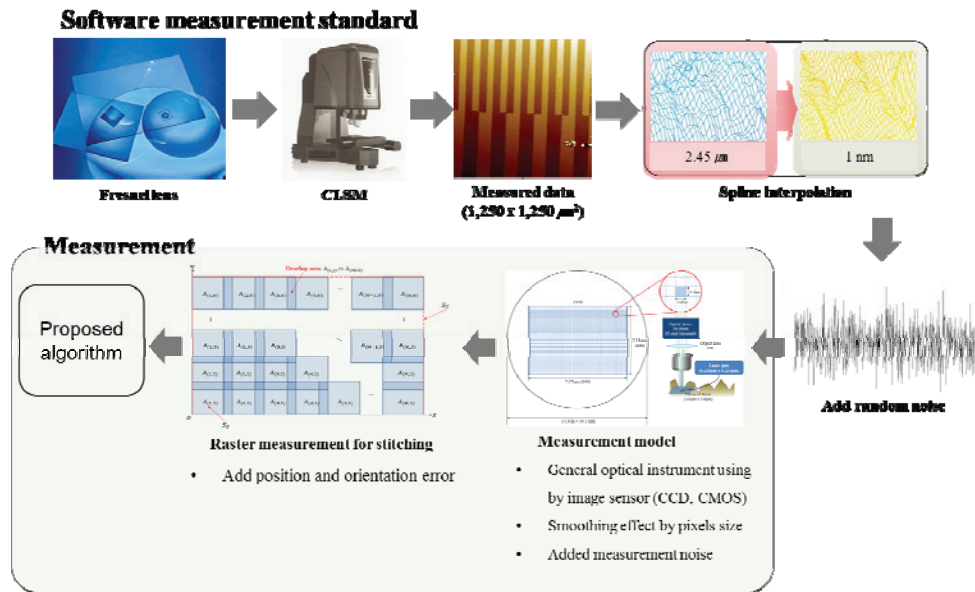


FIGURE 6. Flowchart of the stitching simulation

From the generated virtual specimen, small single measuring data sets were abstracted, which have adjacent areas each other. After abstracting, the different random six degree of positioning errors are added to each of data set to simulate the measuring errors. The overall process is shown in Figure 6. The stitching errors were within 15 nm.

EXPRERIMENTAL RESULTS

For applying the stitching method to measuring data, a measurement system was made, as shown in Figure 7. An active stereo vision system was attached to a three axis stage system to measure a large range by moving the vision system. A Fresnel lens and an aspheric lens were measure by the system. Each specimen was measure four times as moving the vision system.

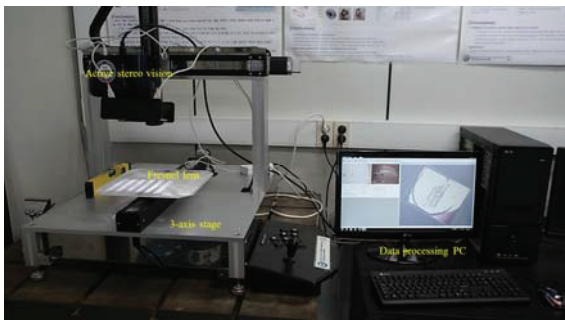


FIGURE 7. Active stereovision system with three-axis stage

The stitched results are shown in Figure 8 and 9. Since lenses need photo developer to be measured by an active stereo vision system, the measured surfaces show some errors from it. However, it is still shown that the stitching was accurately done.

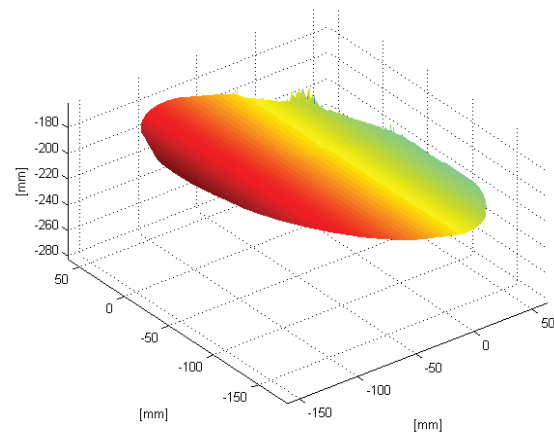


FIGURE 8. Stitched data of the Fresnel lens (2 by 2)

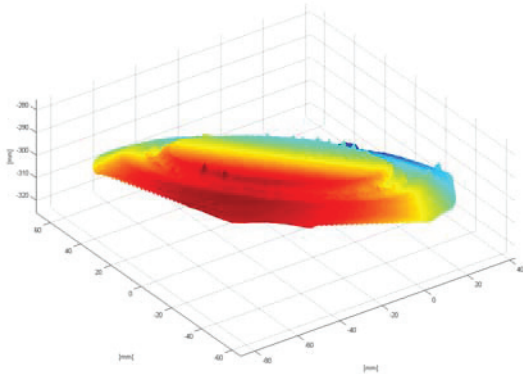


FIGURE 9. Stitched data of the aspheric lens (2 by 2)

A micro lens array was measured by a confocal microscopy and stitched. The result is shown in Figure 10.

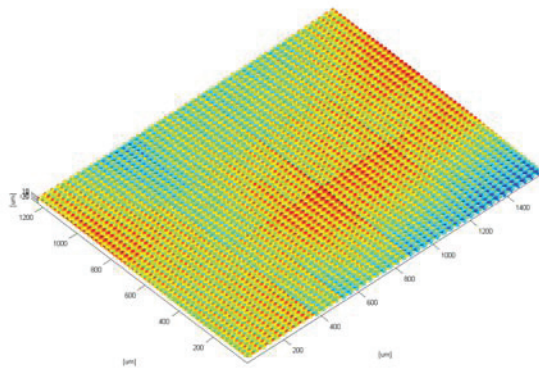


FIGURE 10. Stitched data of the micro lens array (4 by 5)

CONCLUSION

A stitching method using least-squares method, cross-correlation and random extracting method of searching area was proposed. For evaluation of the proposed method, a software measurement standard was generated from a real measured data then a measuring and stitching simulation test was implemented with the software measurement standard. Also, the stitching method was applied to real measured data by an active stereo vision system and a confocal microscopy. For future work, the stitching method is planned to various type of freeform surfaces and the errors are planned to be examine more precisely.

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White light shearing interferometer and its application to examine residual stress of deposited thin films

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ABSTRACT

This paper is to introduce a technique which evaluates residual stresses of thin-films deposited on substrates using a white light shearing interferometer. The interferometer contains a white light source and Savart plate placed on a rotation stage. Where the Savart plate divides the wavefront reflected from the substrate into two laterally displaced ones, the interference pattern generated by the mutual interference of the divided wavefronts is modulated by an envelope function, and the curvature of the substrate is determined by examining the shifting of the darkest fringe of the pattern as the Savart plate is angularly scanned by the rotation stage. Once the curvatures of the substrate before and after thin-film deposition are sequentially determined, the Stoney equation is utilized to calculate the film stress on the substrate. A setup for realizing the interferometer was constructed. And the concept of this technique was verified by conducting this setup to examine the residual stresses of silicon substrates having C₃N thin-films deposited on them.

INTRODUCTION

It is known that the residual stresses due to thin-film coating can cause film crack, film peeling, or substrate warping. Moreover, these stresses may affect the properties of the devices deposited with thin-films, e.g. band-gap, transition temperature, and etc. The measuring techniques implemented for quantitatively evaluating the residual stresses thus received more and more attention. Of which, the x-ray diffraction (XRD) [1-3] and curvature measurement are more popular and acceptable. Where the former one is capable of local stress determinations, but is limited to crystalline films. Whereas the latter detects the average stress of the film under examination without the limitation of XRD.

The technique of curvature measurement is based on the equation published by G.G. Stoney in 1909, and the moire' profilometers[4-6], optical level

meters[7-9], and phase-shifting interferometers [10-13] have been proposed to realize the technique successfully. Where the interferometers possess the merit of high measurement resolution, but suffer from the necessity of anti-vibration system and the noise that may arise during the process of phase unwrapping.

The white light shearing interferometer [14], which was implemented for slope contour measurements, is therefore proposed for curvature measurements in this research. It not only keeps the performance of high measurement resolution but also removes the drawbacks that may exist in the phase-shifting interferometers. In the remainder of this paper, the measurement theory and an installation of the shearing interferometer are introduced sequentially. Subsequently, the experimental results of utilizing the installation to evaluate thin-film residual stresses are presented, and the paper is finally concluded by summarizing the measurement concept of the proposed technique.

MEASUREMENT THEORY

As that shown in Fig. 1, the shearing interferometer is composed of a polarizer (P) with a transmitting axis at 0 deg, a non-polarizing beam-splitter (NPBS), a Savart plate (SP) [15-17], a rotation stage having a rotation axis parallel to the Y-axis, a sample to be examined, an analyzer (A) with a transmitting axis at 90 deg, and a CCD camera. Where the SP is placed on the rotation stage, the principal section of the left plate of the SP is with an angle of 45 deg measured from the X-Z plane, and the surface normal of the SP makes a small angle of $\Delta\alpha$ with respect to the Z-axis.

As the light beam passing through the polarizer and NPBS is incident into the SP at a direction of the Z-axis, it is laterally sheared into two parallel beams (oe and eo beams) which lie on the X-Z plane and have a shear distance of S; the sheared beams travel to the sample and then

return to the SP to get recombined; and the recombined beams are finally guided to propagate through the analyzer to reach the CCD camera.

Note that the recombination laterally shears the wavefront reflected from the sample into two identical ones, the image on the CCD camera is, in fact, a shearing interference pattern which has an intensity of [14, 18]

$$I = \frac{1}{2} V_0^2 (1 - \cos \delta). \quad (1)$$

This is available only for narrowband source; for a broadband source centered at λ_c , it has to be modified as [19]

$$I = \frac{1}{2} V_0^2 (1 - \gamma \cos \delta). \quad (2)$$

Where γ is an envelope function, which reaches the maximum as δ approaches zero, and drops to zero quickly as δ increases. δ is the phase difference between the interference beams, according to Ref.[14], it can be explicitly expressed as

$$\delta = \frac{4\pi}{\lambda_c} (\Delta\alpha - \frac{\partial W(x,y)}{\partial x}) S. \quad (3)$$

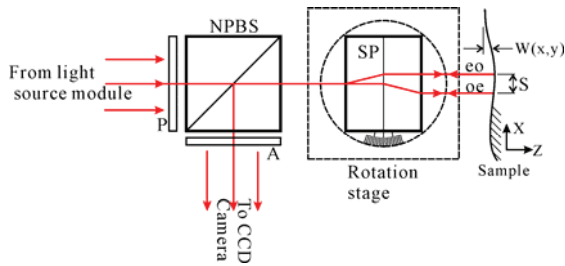


Fig. 1 Schematic diagram of the white light shearing interferometer

The above descriptions indicate that the interference fringe of $\delta=0$ is with minimum intensity (i.e., it is the darkest fringe); and, according to Eq. (3), the surface slope at the location of the darkest fringe has a relation of

$$\frac{\partial W(x,y)}{\partial x} = \Delta\alpha. \quad (4)$$

This implies, the interferometer can command the rotation stage to bring the SP to experience an angular scanning, record the rotation angle corresponding to minimum intensity, and substitute the recorded rotation angle into Eq. (4) to yield the surface slope.

Assume the sample is with an arc shape, the discovered slope can be utilized to determine the curvature of the sample, which has a form of

$$\frac{1}{R} = \frac{\Delta\alpha_b - \Delta\alpha_a}{x_b - x_a}. \quad (5)$$

Where $\Delta\alpha_a$ and $\Delta\alpha_b$ are the slopes at coordinates x_a and x_b , respectively, of the sample. We further assume that the sample curvatures before and after thin-film coating are determined as $1/R_1$ and $1/R_2$, respectively. The film stress, σ , can be yielded using Stoney equation [4-13], and

$$\sigma = \frac{1}{6} \frac{E_s}{(1-\nu_s)} \frac{t_s^2}{t_f} \left(\frac{1}{R_2} - \frac{1}{R_1} \right). \quad (6)$$

Where E_s and ν_s are Young's modulus and Poisson's ration, respectively, of the sample; t_s and t_f are the thicknesses of the sample and deposited film, respectively. The technique proposed in this research is to measure the sample slopes using the shearing interferometer shown in Fig. 1 and then extract the residual stresses of the films via the uses of Eqs. (5) and (6).

EXPERIMENTAL SETUP

A setup composed of a light source module, the shearing interferometer shown in Fig. 1, and a control and image processing system was constructed to achieve the of film stress measurements.

The light source module comprises of a halogen lamp and three lenses. It radiates a nearly collimated light beam to the shearing interferometer.

In the shearing interferometer, the SP is made of calcite crystal, and it has a shear distance of $S=1.5\text{mm}$; the rotation stage is NS5311-C from Nano Control Co., it is equipped with a PZT actuator and capacitive sensor, and it has an angular positioning resolution of 0.01 arc-sec.

The control and image processing system contains a frame grabber, a stage controller/driver, and a personal computer. The computer captures the intensity patterns mapped on the CCD camera via the frame grabber, communicates with the rotation stage through the use of the stage controller/driver, and executes the programs of scanning and measurement.

The scanning commands the computer to send an analog signal to drive the stage to undergo an angular scanning. The measurement extracts the minimum intensity and the corresponding rotation angle of each point while the stage is scanning,

and it determines the sample slope of each point by the use of the recorded rotation angle and Eq. (4). And the measurement determines the curvatures of the sample using Eq.(5) and the film stresses using Eq.(6).

EXPERIMENTAL RESULTS

The samples were rectangular silicon substrates having dimensions of $w_s \times h_s \times t_s = 30 \times 10 \times 0.3 \text{ mm}$ and mechanical properties of $E_s = 150 \text{ GPa}$ and $\nu_s = 0.17$. The experimental setup was conducted to measure the curvatures of each sample before and after C_rN thin-film coating, and it then plugged the measured curvatures into Eq. (6) to yield the film stress of the sample.

The samples were deposited with films of different thicknesses, the measured residual stress with respect to film thickness is exhibited in Fig.2, this result demonstrates that shearing interferometer is capable of film stress evaluations.

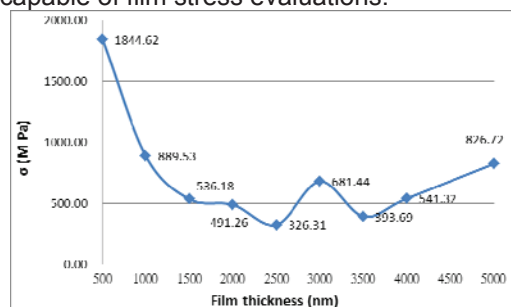


Fig. 2 The measured residual stress with respect to film thickness

CONCLUSION

A white light shearing interferometer was introduced to evaluate residual stresses of thin-films deposited on substrates in this paper. A mathematical model of the developed methodology was derived to demonstrate the basic concept of the technique. An experimental setup was established to accomplish the shearing interferometer, and the experimental results from using this setup confirm capability of thin-film stress evaluations.

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The Design, Manufacture and Application of Sub-micron Grating in Color Separation for Display Backlight System

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INSTRUCTIONS

Liquid crystal displays (LCDs) are widely used for a variety of information displays. But, the light efficiency of LCD is very low due to the absorption of color filter. In the latest developments, the use of diffraction gratings to couple out the lights from the light guide in order to replace color filter had been proposed by many researchers. Dammann used a sandwich grating structure to separate colors [1]. Taira et al. demonstrated that a lithography molded diffraction grating can be used to separate colors. In his study, a 530 nm-pitch grating on a wedge light guide was combined with a cylindrical lens array to produce red-green-blue (R-G-B) colors for a display [2-4]. Caputo et al. also used a holographic structures and a lenticular lens array to realize the R-G-B-G pixel array [5-7]. However, the fabricated grating is generally smaller than 1 cm² in area, limited by the E-beam lithography. Lin et al. used a roll-to-roll process to produce 4 μm-pitch grating film and a high collimation backlight to realize a large-area color-filterless display [8]. Based on a similar concept, Chen et al. introduced a blazed grating embedded into the LCD to separate the left and right images for forming a color stereoscopic image without using a dye color filter [9].

The object of this study is to design a color separation module with sub-micron grating. The roll-to-roll imprinted process is adopted to fabricate the sub-micron grating and lens array with the benefits of mass production, lower manufacturing cost and more friendly to the environment.

METHODS

The color separation module can be divided into three parts: the light guide, the sub-micron grating and the lens array. For the light guide design, the uniformity of light illumination on the top surface of the light guide is mainly affected by light guide angle, θ_1 . In order to reach a uniform light illumination the following equations should be followed:

$$\theta_1 = \cos^{-1} \frac{1 + \sqrt{1 + 8n_L^2}}{4n_L} \quad (1)$$

$$\theta_2 = 90^\circ - 2\theta_1 \quad (2)$$

$$\theta_{inc} = \theta_1 + \theta_2 \quad (3)$$

All parameters are shown in Fig. 1 and n_L is the refraction index of light guide. The equations are derived based on the assumption that the light incident into the light guide horizontally. The desired angles of θ_1 , θ_2 and θ_{inc} calculated are 26.73°, 36.54° and 63.27° respectively.

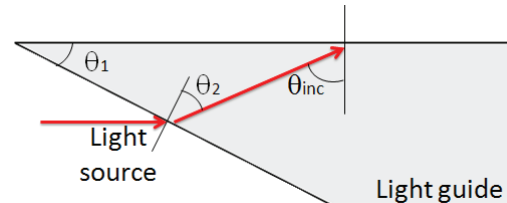


FIGURE 1. Parameter definition for light guide design

In the grating design, the grating pitch is designed to vertically extract red light (621nm). With the parameters chosen previously, the grating pitch is optimized by using the following grating equation:

$$m\lambda = \Lambda(n_0 \sin \theta_{dif} - n_g \sin \theta_{inc}) \quad (4)$$

Where $\theta_{inc} = 63^\circ$ is the incident angle, θ_{dif} is the diffracted angle, $n_0 = 1$ is the incident medium's refractive index, Λ is the pitch, $n_g = 1.5$ is the refractive index of the diffracted medium and $m = -1$ is the diffraction order.

Fig.2(a) showed the schematic configuration of the backlight system. A mirror was added to recycle the lights that may emitted from the right hand side of the light guide. But, the diffractive lights from the incident direction will mix with those recycled. To solve this problem, the grating pitch has to be selected properly. With the known incident angle, the refractive index and the diffraction order, the diffraction angle θ_{dif} was determined by grating pitch and can be estimated by Eq. (4). With a derived pitch of 464nm and the arrangement of lens array, the diffractive lights may appear in the following order: B/G/R/G/B, instead of mix up, as shown in Fig.2(b). The period of the cylindrical lens was 600 μm , which is equal to the full width of three sub-pixels. The radius of curvature for the cylindrical lens was 608 μm . Fig.3 shows the simulated result of the B/G/R/G periodical line pattern.

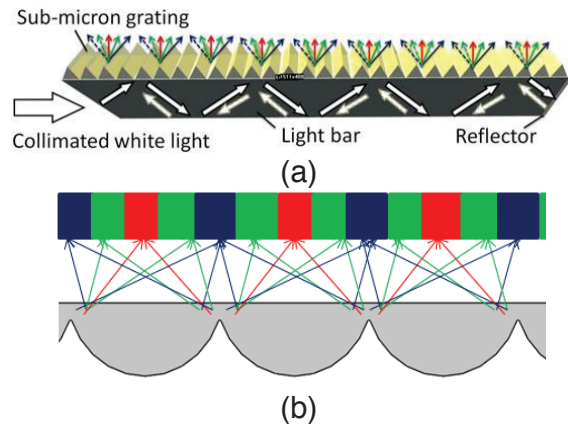


FIGURE 2. (a) The color separation module to improve diffraction efficiency. (b) The B-G-R-G patterns by using lens array.

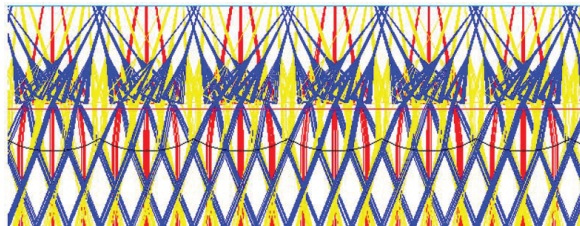


FIGURE 3. The simulation of the lens array on the light guide.

EXPERIMENTAL RESULTS

The sub-micron grating film was made via a roll-molding process. The mold is machined using a single point diamond turning process. The single point diamond tool possesses the characteristics of high hardness, high heat conduction rate, low friction coefficient and low heat expansion coefficient, so it can match the requirement of high accuracy and low surface roughness in optics. After the mechanically grooved structure was formed on an imprinting roller using a diamond tool with the designed profile, the ultraviolet (UV) embossing equipment was applied: the UV resin was first dispensed on the PET film, imprinted by the roller, hardened by UV curing, and demolded from the roller to obtain the gratings on a 188 μm thick PET film (T-A4300 by Toray Corp.). The schematic diagram of the process is as shown in Fig. 4.

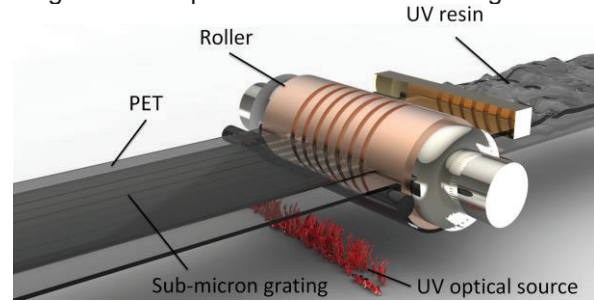


FIGURE 4. The simulation of the lens array on the light guide.

The scanning electron microscopy (SEM) image of the sub-micron grating was shown in Fig. 5 (a). The period of grating measured using SEM was within 464 ± 15 nm. The roll-to-roll process was also used to fabricate the cylindrical lens array, of which the curvature radius of each lens was 608 ± 3 μm (Fig. 5 (b)). The wavelength of collimated LED light sources was 621nm (R), 523nm (G), and 448nm (B). A mirror was placed next to the tested module to reflect lights back. The CCD was set in the front of the tested module to examine the light distribution of the tested module. Fig. 6 showed the sequential light patterns from the tested module. The color distribution followed the order designed in a repeated order B/G/R/G. The blue lights and red lights are overlapped well and the color bands match the width of sub-pixel also. And then, the standard of National Television System Committee (NTSC) was used to evaluate the performance of the designed color separation module. In the beginning, the color gamut, the R, G, B LED light sources, were adjusted to create a white incident light D65,

which was frequently used as a white light definition. The points 1, 2 and 3 in Fig. 7 indicated the xy coordinate of individual R, G and B LEDs, and its color gamut was about NTSC 121%. While the point 4, 5, 6 and 7 stands for the test result for the red, green, blue and green color bands shown in Fig. 7 respectively. From the test results, the color gamut for the RGB colors diffracted out of grating simultaneously can reach around NTSC 100%.

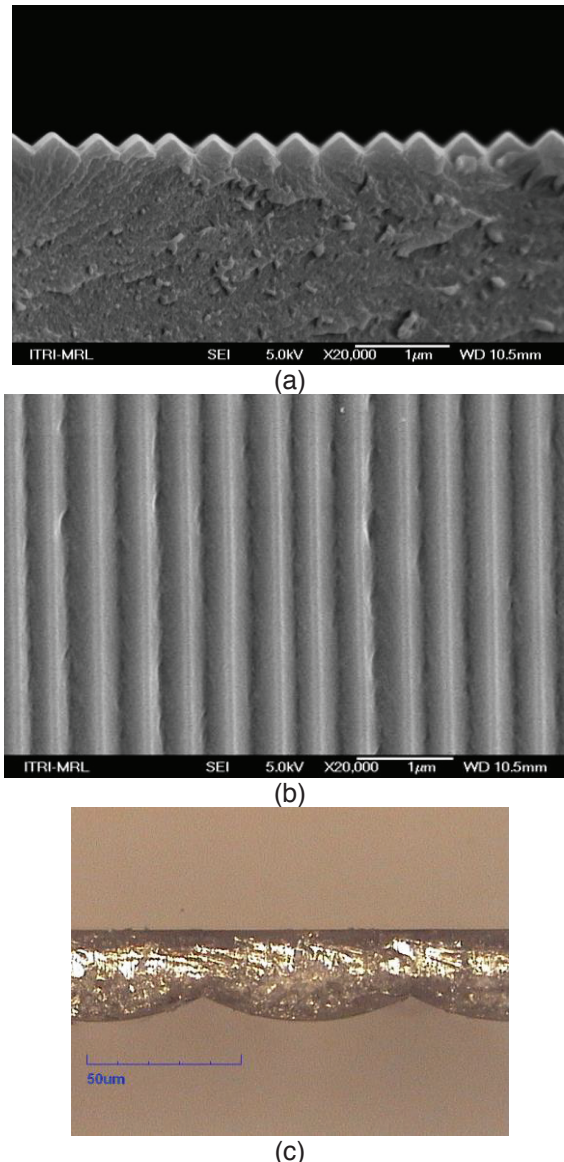


FIGURE 5. SEM image of the (a) top view and (b) cross-sectional profile of the sub-micron grating and (c) cross-sectional profile of the lens array.

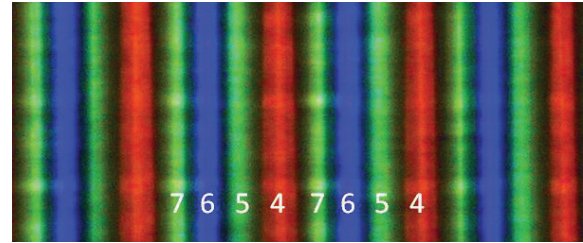


FIGURE 6. The sequential light pattern of the tested module

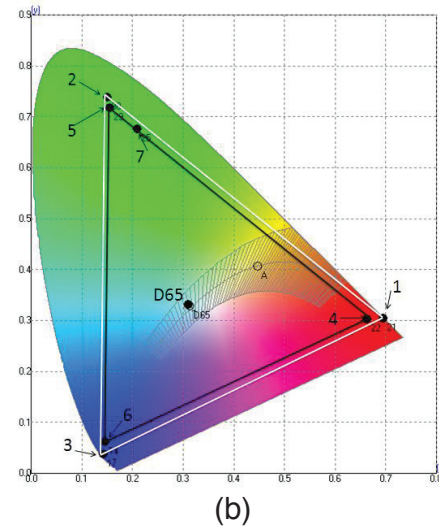


FIGURE 7. CIE chromaticity diagram of the tested module

CONCLUSIONS

This study used a roll-to-roll based UV resin process to fabricate sub-micron grating for display applications. Comparing with the e-beam and the interferometry, the roll-to-roll process had the advantages of mass production, lower manufacturing cost and more friendly to the environment.

The proposed color separation module contained a light guide, a sub-micron grating film, and a lens array. It successfully separated white lights and displayed different line patterns into the sub-pixels. Instead of diffracting the green light normally out of the light guide [4-6], a repeating B-G-R-G array was designed as the sub-pixels for the consideration of overall efficiencies, diffracted angles on the different wavelengths and feasibility of the fabrication. With the collimated lights design, a larger color gamut was also achieved.

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Experimental formulation of capillary force between an adhesional wetting surface and an immersional wetting end of rod

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INTRODUCTION

Recently, miniaturization of portable devices and their electronic parts has been remarkable. Consequently, the need for micro manipulation to make more complex and smaller devices is increasing. One of major devices to pick up micro electronic parts is a vacuum pick-up nozzle which generates sufficient force and contamination-free. However, it still has 4 main problems. First, it can't hold complex-shaped objects such as wires and springs because of an air leak. Second, it can't hold less than 0.15mm objects because it is difficult to control the negative pressure when an inner diameter of the nozzle becomes less than 0.1mm. Third, the nozzle is not suitable for picking up fragile parts because of a solid contact between the part and the nozzle. Forth, it causes a sticky problem; the micro objects are easily trapped by adhesive forces, such as electrostatic force, interfacial force, viscous drag force which is normally negligible over cm-sized objects. Mechanical tweezers is also major to pick up objects. However, it also has the sticky problem. We have focused on capillary force to pick up micro objects. There are many study examples of picking up micro objects by capillary force. [1][2][3] We have developed a new device called "Wet tweezers" to solve these problems over the past few years. The wet tweezers can adapt to complex-shaped objects because the liquid changes its shape flexibly according to any shape of the contact surface. And it doesn't have the sticky problem. We have already checked that the wet tweezers can pick up 1mm-squared electronic parts. However, we have not formulated the capillary force because it has unique boundary condition. The capillary force between two adhesional-wetting solids is well-known. However, the wet tweezers makes a liquid bridge between an adhesional-wetting surface and an immersional- wetting end of the rod. That is unique boundary condition because "contact angle" is not exist at the end of the rod; an edge angle of the meniscus at the end of the rod can change due to a distance between the

adhesional-wetting surface and the end of the rod. That makes the estimation of the amount of the capillary force difficult because we need the relationship among the distance, the capillary force, and the edge angle experimentally and/or theoretically. We have observed the shape of liquid bridge and the capillary force measured by electric balance simultaneously at the various distances. Then, we have formulated the shape by using geometrical approximation. To check the accuracy of the formulation, we have compared measured value of the capillary force with the calculated value from the formula. We have also discussed interesting experiments using a gel-coated rod. [4]

MICRO LIQUID DISPENSER

In this section, we describe micro liquid dispenser. Before we put micro objects by using wet tweezers, we assume that we need to stamp a tiny liquid drop on its exact area to place it. That was the reason why we invented the micro liquid dispenser as shown in Figure 1. It is composed of a single rod driven by a linear motor in z axis and a thin tube in which the liquid is trapped by capillary force. Figure 2 shows the sequence of dispensing of liquid drops. The single rod simply penetrates through the liquid and stamp the liquid to a basal plate repeatedly.

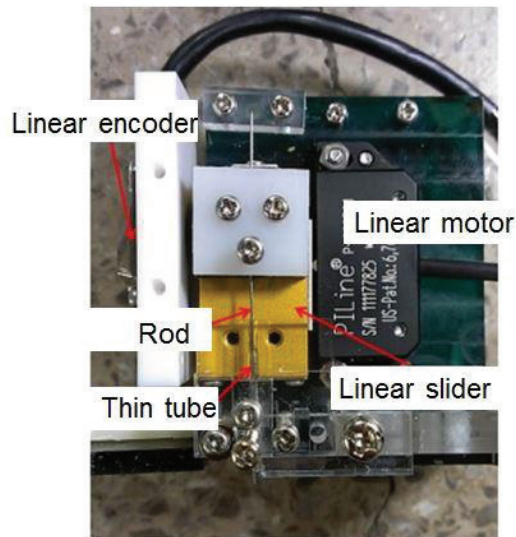


FIGURE 1 Photograph of the dispenser to stamp liquid

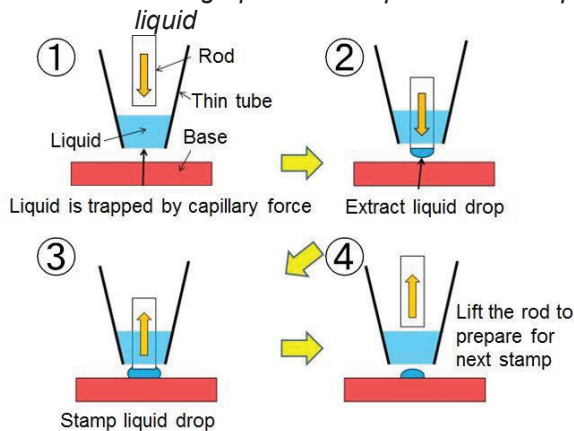


FIGURE 2 Sequence of dispensing of liquid drops.

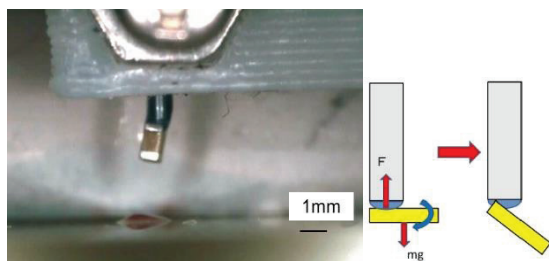


FIGURE 3 Inclination of a chip part during picking it up by the single-rod.

When we applied this single-rod dispenser for picking up micro objects by its capillary force, the picked objects easily inclined by slight vibrations as depicted in Figure 3.

WET TWEEZERS

We have remodeled this dispenser to suit for picking up micro objects without the inclination. Figure 4 shows “Wet tweezers”. It is composed

of a pair of rods driven by a linear motor and two linear sliders in z axis and a pair of thin tubes in which liquids are trapped by capillary force. Figure 5 shows its sequence of picking up micro objects.

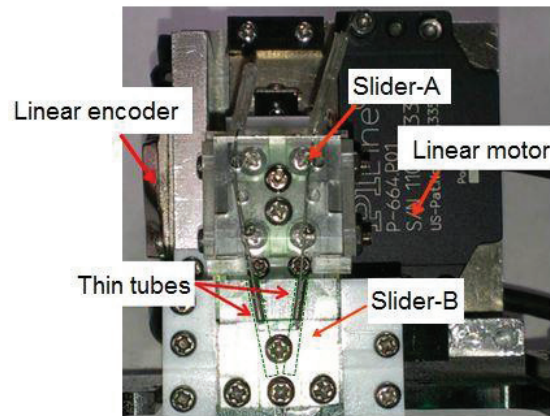


FIGURE 4 Photograph of “Wet Tweezers”

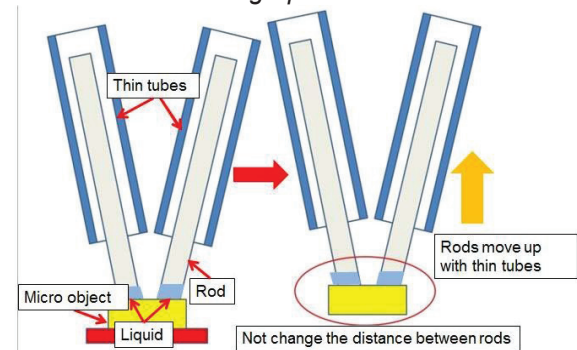


FIGURE 5 Sequence of picking up micro objects by the wet tweezers

If we contact both ends of the two rods to an object, we can pick it up by capillary force. Posture of the captured object is fixed when we use the pair of the rods. Moreover, the wet tweezers adapts to complex-shaped objects because the liquid changes its shape flexibly according to any shape of the contact surface. We have already checked that the wet tweezers can pick up 1mm-squared electronic parts. [5] In this report, we have formulated the capillary force under the unique boundary condition of adhesional and immersional wetting surfaces.

FORMULATION OF THE CAPILLARY FORCE

The formula of capillary force between adhesional wetting surfaces is well-known. Figure 6 shows the liquid bridge between adhesional wetting surfaces under quasi-static condition. z is the gap distance between the surfaces. R is the radius at the horizontal section of the meniscus. R is the function of z

and the height of the cross section, H . ρ is the radius of curvature at the vertical section of the meniscus. ρ is the function of z and H . θ is the contact angle of the bottom surface. α is the contact angle of upper surface. α is constant under the boundary condition. ρ is curvature of the curved line of the vertical section of the meniscus, $R(H, z)$ as given by,

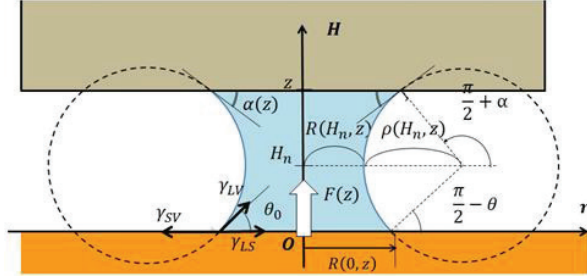


FIGURE 6 Geometric model of the liquid bridge between adhesional wetting surfaces

$$\rho(H, z) = \frac{\left\{1 + \dot{R}(H, z)^2\right\}^{\frac{3}{2}}}{\ddot{R}(H, z)}. \quad (1)$$

$\dot{R}$ and $\ddot{R}$ are partial differentiated with respect to H . Laplace pressure P at H_n and z is defined as given by,

$$P(H_n, z) = \gamma \left(\frac{1}{R(H_n, z)} - \frac{1}{\rho(H_n, z)} \right). \quad (2)$$

γ is the surface tension of the liquid. Laplace pressure is caused by the unbalance of the surface tension caused by the unevenness of the pair of curvatures. Capillary force for picking up bottom surface, or liquid bridging force, is represented as [6],

$$F(H_n, z) = 2\pi\gamma R(0, z) \sin\theta - \pi\gamma R(H_n, z)^2 \left(\frac{1}{R(H_n, z)} - \frac{1}{\rho(H_n, z)} \right). \quad (3)$$

The boundary condition of wet tweezers is not both side adhesional wetting. That is an adhesional-wetting surface and an immersional-wetting end of the rod. That is unique boundary condition because “contact angle” is not exist at the end of the rod; an edge angle of the meniscus at the end of the rod can change due to a distance between the adhesional-wetting

surface and the end of the rod. That makes the estimation of the capillary force difficult because we need the relationship among the distance, the capillary force, and the edge angle experimentally and/or theoretically. To investigate the relationship, we have observed the shape of liquid bridge and measured the amount of capillary force by electric balance simultaneously at the various distance. Then we have formulated the shape of meniscus by using geometrical approximation. We have approximated the shape of liquid bridge as the circle and quadratic function. We have deformed the function of capillary force into the function of two variables, z and α . Figure 7 shows the plot of the gap distance between the bottom surface and the rod and the measured and the approximated theoretical values.

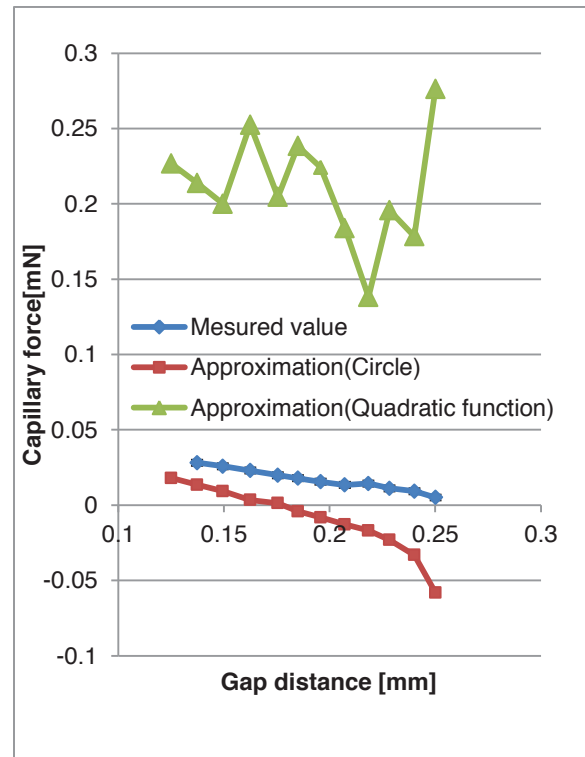


FIGURE 7 The plot of the gap distance and the measured and the theoretical values.

The discrepancy between measured and approximated theoretical values is more than 200%. There are two main reasons why the discrepancy is large. First reason is the geometrical error caused by the circle and quadratic approximations of the meniscus. Second reason is the gradual dripping of the liquid along the rod by gravity force.

GEL ROD

To solve the two problems of the wet tweezers, we have newly proposed “gel rod”. Because gel can absorb plenty of liquid, we assume that we can solve the dripping problem by gel. To check the feasibility, we fabricated a simple thin rod made by gel. As shown in Figure 8, we succeeded in picking up quadrangular pyramid by the gel rod with a diameter of 1mm. Note that the quadrangular pyramid is difficult for picking up by the conventional air nozzles used in surface mounting placement machine. We have also confirmed that the gel rod keep capture micro objects over 30 minutes at 20 degrees C and 40% relative humidity.

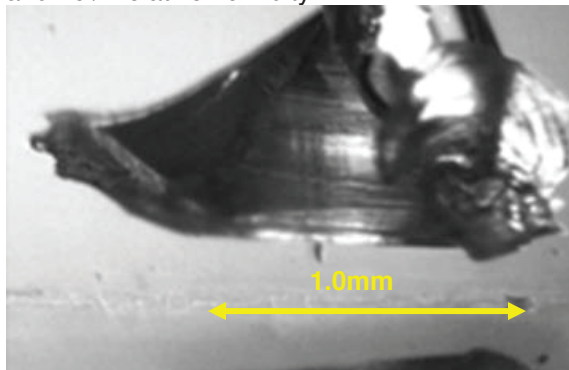


FIGURE 8 Photograph of the picked quadrangular pyramid by gel rod

CONCLUSION and PERSPECTIVE

We have reported that development of the capillary-force based manipulator of “Wet tweezers” for picking up micro objects. We have studied the modeling method of the capillary force. However, we found that it was difficult to formulate the capillary force because of the geometrical errors of the meniscus and the gradual dripping of the liquid along the rod. To make the manipulator more robust, we have newly proposed and tested “gel rod”. In experiments, we confirmed the gel rod can capture micro objects over 30 minutes at 20 degrees C and 40% relative humidity. Future works of this study are making the wet tweezers with a pair of gel-coated rods and checking its performance of capturing complex -shaped object and micro parts.

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Development of an integrated 3DoF inner position sensor by 4 encoders with 0.5 μm resolution for precise holonomic inchworm robot

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INTRODUCTION

Recently, portable devices such as smart phone and tablet PC become popular and mass-produced at a low cost. Inside those portable devices, there are numerous various parts such as MEMS sensor and electronic chip parts with less than 1mm^3 . Those tiny parts are mounted precisely with 0.05mm assembling accuracy by conventional Surface Mounting Technology (SMT) placement machine. Inside the SMT placement machine, multi-axes linear stages are used for positioning of those electronic chip parts. Those multi-axes linear stages have sufficient positioning accuracy and velocity. However, their weights are over 100kg and their big inertia forces generate big vibration around the SMT placement machine. In contrast, the weight of electronic chip parts is less than 1mg, so almost all energy of the SMT placement machine is used for moving multi-axes stages. There are a lot of research activities about mobile wheeled robots. Those robots can move wide area omnidirectionally and they are relatively lightweight compared with multi-axes stages. However, it's difficult for those robots to realize precise positioning with micro to nanometer accuracy because of their backlash. In those ten years, we have developed 3-degrees-of-freedom (3DoF) mobile mechanism with less than 10nm positioning resolution. In various experiments, we have concluded that one of the breakthrough technologies for practical use of them is how well we can obtain sufficient positioning repeatability and accuracy. Particularly, precision and accuracy of the last one step is very important for precise work. Visual feedback technology with CCD-camera is powerful method to measure the robots'

positions, however the measuring speed is only 0.1 second if we use 500M-pixel-camera. That is the reason why the visual feedback technology is not good enough for controlling the robot high speed because we cannot measure velocity with sufficient resolution. Optical linear encoders have attracting performances because their measuring speed is very fast, over 1m/s, with sufficient measuring resolution, less than 0.5 μm . However, there are almost no reports that show the design of integrated three-degrees-of-freedom (3DoF) inner position sensors within one step-motion for those precise mobile mechanisms. In this research, we present a newly proposed design of the integrated 3DoF inner position sensor. We also explain how to obtain 3DoF displacements from the four measured distances. In experiments, we move the robot 20 μm -rectangular-shaped path with 0.5 μm positioning resolution. We have confirmed that the proposed inner sensor has sufficient feasibility to improve the positioning accuracy of the omnidirectional and holonomic inchworm mechanisms. We also discuss about the potential benefit of flexible, energy-efficient, vibration-less, and multi-functional miniature robots factory in bio-medical, nanoscience, and chip-mounting technologies collaborated by those compact mobile mechanisms.

3DoF STRUCTURE AND PERFORMANCE

Fig. 1 shows the structure of the mechanism. *EM-1* and *EM-2* are two Y-shaped electromagnets (EMs) that are separate to avoid attracting each other. These Y-shaped EMs make a closed loop via a ferromagnetic surface to obtain enough magnetic force. *PZT-F*, *PZT-B*, *PZT-L1*, *PZT-L2*, *PZT-R1*, and *PZT-R2* are the

six Piezoceramic Actuator(PAs).We arrange *EM-1* and *EM-2* to cross each other and connect to the six PAs so that the mechanism can precisely move in any direction by the inchworm principle. Fig. 2 shows a photograph of the mechanism. The mechanism is 100 g, 86 mm in length and width, and 11 mm in height. The mechanism has parallel leaf springs for smooth contact of all the legs on the surface simultaneously. Table 1 shows the typical performance of the mechanism. We used APA50XS “Moonie” PAs (Cedrat Inc.) connected in a series. Table 2 shows the typical performance of the PAs. Fig. 3 shows the motion sequence of orthogonal

TABLE I
PERFORMANCE OF THE INCHWORM MOBILE MECHANISM

Quantity	
Step length (120 V)	0.1~62 μm
Resolution (15-25°C, Less than 50% rH)	Better than 10 nm
DoF	X,Y, θ
Natural Frequency (blocked free)	500~600 Hz
Maximum Velocity (Frequency)	~5 mm/s (100Hz)
Repeatability (CV; Ratio of SD of positioning error to a path length) (Frequency)	~1% (100Hz)
Thrust force	1.5 N
Pay load	150 g
Height x Thickness x Length	86 x 86 x 15 mm
Weight	100 g

TABLE II
PERFORMANCE OF THE PIEZOELECTRIC ACTUATORS

Quantity	
Displacement (100 V)	95.5 $\pm$ 5 μm
Generative Force (100 V)	18.0 N
Spring constant	115,000 N/m
Capacitance	1.04 μF
Resolution (15-25°C, Less than 50% rH)	1.52 nm
Natural Frequency (blocked free)	1.45 kHz
Height x Thickness x Length	12.9 x 6.4 x 9.2 mm
Weight	4 g

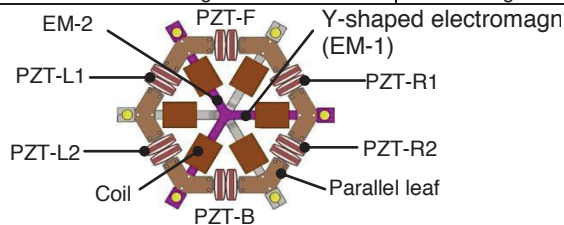


Fig.1 Structure of the holonomic inchworm robot

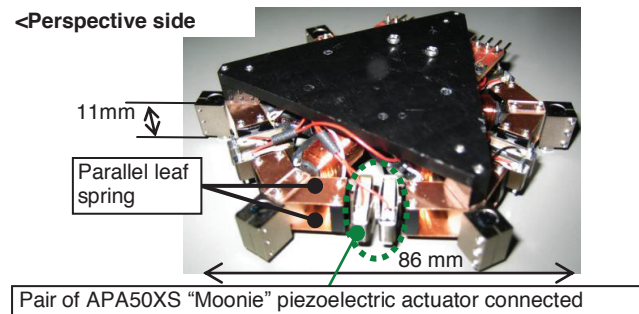


Fig. 2 Photograph of the holonomic inchworm robot

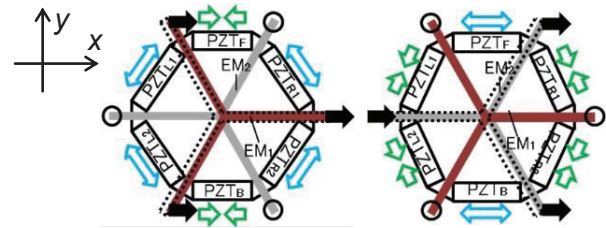


Fig. 3. Principle of inchworm motion (rightward movement)

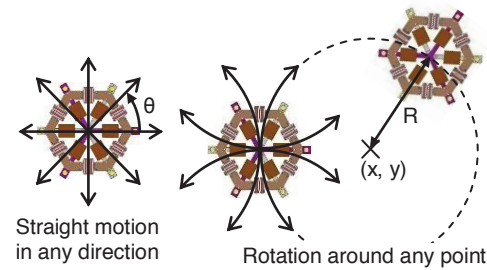


Fig. 4. Motion patterns

motions in the x-axis. This mechanism moves like an inchworm while retaining the synchronism among the rectangular-shaped forces of the two EMs and vibrations of the six PAs. This mechanism enables the robot to move precisely in any 3DoF motion with a resolution of better than 10 nm by changing the amplitude of the vibrations. As shown in Fig. 4, the mechanism moves straight in any direction and rotates around any point precisely. This small mechanism moves flexibly and widely on well-polished ferromagnetic surfaces. Note that a minimum step width of the inchworm motion is around 100 nm as shown in Table 1 because of a disturbance generated by switching of the EMs. However, when we keep one EM fixed to the floor, we have checked that the other can be positioned precisely better than 10 nm of the resolution from experiments because the lower limit of the resolution is determined 1.52 nm of the PA's resolution under the condition listed in table 2.

3DoF MEASURING PRINCIPLE BY 4 LINEAR ENCODERS

We will now explain how to convert the four measured distances into 3DoF motion of free electromagnet. As depicted in Fig. 5, we draw a vector analysis when *EM-1* is fixed to the floor and *EM-2* is moved by the actuators. In this case, the encoders move, and the scales are fixed to the floor. 3DoF motion of *EM-2* is divided into its translational motion and

rotational motion. Due to space limitations, we present only the important equations. The entire equation set is available upon request from the author. E_1 is the initial position of encoder-1.

E_1' is the position of encoder-1 after it moves a distance ΔL from the initial position. Here, ΔL is defined as a vector component of the translational movement of $EM-2$ as shown by $\Delta L \equiv W \begin{pmatrix} \cos\varphi \\ \sin\varphi \end{pmatrix} \equiv \begin{pmatrix} \Delta X \\ \Delta Y \end{pmatrix}$. (1)

W is the travel distance of ΔL , and φ is the travel direction of $EM-2$. We define the X-coordinate and Y-coordinate of E_1 as X_{10} and Y_{10} , respectively. $\Delta\theta$ is defined as a posture change of $EM-2$. $\Delta\theta$ is smaller than 0.002 radian because the maximum displacements of the piezoelectric actuators are less than 100 μm , which is much smaller than the rotational radius of the encoders, R , which is 50 mm. E_1'' is the position of encoder-1 after we move it by ΔR_1 from the position of E_1' . We define the X-coordinate and Y-coordinate of E_1'' as X_1 and Y_1 , respectively. Here, we define ΔR_1 as

$$\Delta R_1 = R \begin{pmatrix} \cos\Delta\theta & -\sin\Delta\theta \\ \sin\Delta\theta & \cos\Delta\theta \end{pmatrix} \vec{E}_1' - \vec{E}_1'. \quad (2)$$

Other parameters are also defined similarly to the parameters related to encoder-1. In fig. 6, we omit parameters related to encoder-4 for simplification. We obtain the following relationship between the initial positions and final positions of the encoders

$$\begin{pmatrix} X_k \\ Y_k \end{pmatrix} = \begin{pmatrix} \cos\Delta\theta & -\sin\Delta\theta \\ \sin\Delta\theta & \cos\Delta\theta \end{pmatrix} \begin{pmatrix} X_{k0} \\ Y_{k0} \end{pmatrix} + W \begin{pmatrix} \cos\varphi \\ \sin\varphi \end{pmatrix} \quad (3)$$

Here, k equals 1, 2, 3, and 4. We find the following initial conditions from Fig. 6

$$\begin{pmatrix} X_{10} & X_{20} & X_{30} & X_{40} \\ Y_{10} & Y_{20} & Y_{30} & Y_{40} \end{pmatrix} = \begin{pmatrix} R & 0 & -R & 0 \\ 0 & R & 0 & -R \end{pmatrix} \quad (4)$$

We know that the measurable parameters are Y_1 , X_2 , Y_3 , and X_4 by the four encoders. From (3) and (4), we obtain

$$\begin{cases} \begin{pmatrix} \Delta X \\ \Delta Y \end{pmatrix} = \frac{1}{2} \begin{pmatrix} X_2 + X_4 \\ Y_1 + Y_3 \end{pmatrix} \\ \Delta\theta = \sin^{-1} \left(\frac{Y_1 - Y_3}{2R} \right) = \sin^{-1} \left(\frac{X_4 - X_2}{2R} \right) \end{cases} \quad (5)$$

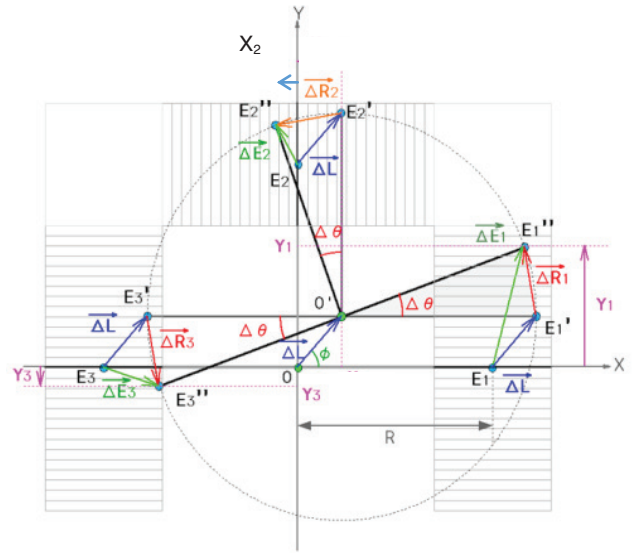


Fig. 5. Vector solution when sensors move; $EM-2$ moves and $EM-1$ is fixed

3DoF DYNAMIC MODEL

As depicted in fig. 6, we define the 3DoF dynamic model of the mechanism when $EM-2$ is free and $EM-1$ is fixed. Here, k_L and k_S are the spring constants of the six PAs in compression and shear deformations, respectively. c_S is the damping constant of the actuators in shear deformation. d_F is the enforced displacement of the piezoelectric actuator of $PZT-F.d_B$, d_{R1} , d_{R2} , d_{L1} , and d_{L2} are similarly defined. We assume that $EM-1$ and $EM-2$ are rigid bodies. $EM-1$ and $EM-2$ are connected by hinged joints at P_1 , P_2 , and P_3 . O_1 , O_2 , and O_3 are the initial positions of P_1 , P_2 , and P_3 , respectively. x_1 and y_1 are coordinates of P_1 ; x_2 , y_2 , x_3 , and y_3 are similarly defined P_2 , P_3 . P_{G1} is the center of gravity of $EM-1$. The position of P_{G1} is represented by an orthogonal coordinate system used by x_{G1} and y_{G1} , and P_{G2} is similarly defined by x_{G2} and y_{G2} . O_{G1} is the origin point of P_{G1} . The six PAs move P_1 , P_2 , and P_3 . 3DoF motion of $EM-2$ is determined by the positions of P_1 , P_2 , and P_3 ; thus, the PAs move the free leg, $EM-2$. $\vec{F}_r$ is a frictional force vector acting on $EM-1$ from the floor surface. F_x and F_y are coordinates of $\vec{F}_r$ in the X- and Y-axes, respectively. N_f is the moment of force acting on $EM-1$ from the frictional force. m is defined as each of the mass of $EM-1$ and $EM-2$. I is defined as each of the moment of inertia of $EM-1$ and $EM-2$ about their own gravity centers. r is defined as a distance between mechanical center and an end of EM. When $EM-1$ is in static condition, $\ddot{x}_{G1}$, $\ddot{y}_{G1}$ and $\ddot{\theta}_{G1}$ are 0. From Fig. 6, Newton's equations of motion for $EM-1$ and $EM-2$ in the x-, y-, and θ -axes are represented as,

$$\begin{bmatrix} m\ddot{x}_{G2} \\ m\ddot{y}_{G2} \\ I\ddot{\theta}_2 \end{bmatrix} = -3(k_s + k_L) \begin{bmatrix} x_{G2} \\ y_{G2} \\ \frac{r^2}{2}\theta_2 \end{bmatrix} - 3c_s \begin{bmatrix} \dot{x}_{G2} \\ \dot{y}_{G2} \\ \frac{r^2}{2}\dot{\theta}_2 \end{bmatrix} - k_L \begin{bmatrix} d_F + d_B - \frac{1}{2}(d_{L1} + d_{L2} + d_{R1} + d_{R2}) \\ \frac{\sqrt{3}}{2}(-d_{L1} + d_{L2} + d_{R1} - d_{R2}) \\ \frac{\sqrt{3}}{2}r(-d_F + d_B + d_{L1} - d_{L2} + d_{R1} - d_{R2}) \end{bmatrix} \quad (6)$$

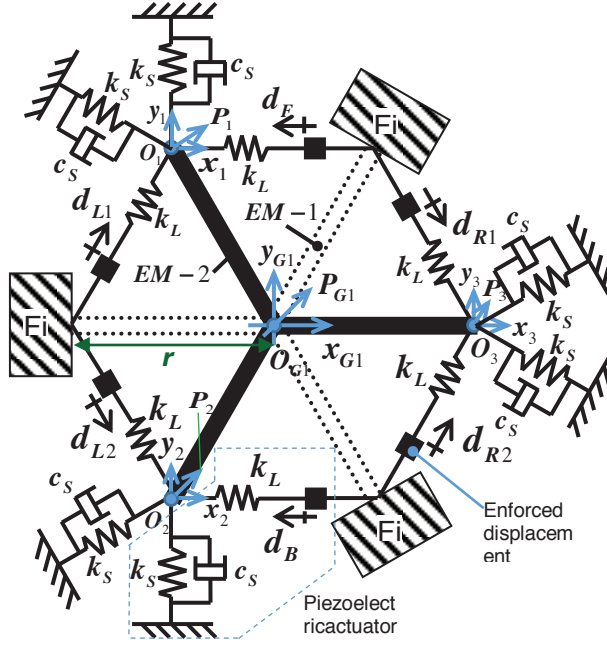


Fig. 6. 3DoF dynamic model

STATE SPACE REPRESENTATION

We assumed that the displacements of the actuators are approximately proportional to their input voltages, which we defined as the proportional coefficient, h . Therefore, we can calculate the input voltage to the actuator, $V(t)$, as follows:

$$\mathbf{V}(t) \equiv \begin{bmatrix} V_F(t) \\ V_B(t) \\ V_{L1}(t) \\ V_{L2}(t) \\ V_{R1}(t) \\ V_{R2}(t) \end{bmatrix} \equiv h \begin{bmatrix} d_F(t) \\ d_B(t) \\ d_{L1}(t) \\ d_{L2}(t) \\ d_{R1}(t) \\ d_{R2}(t) \end{bmatrix} \quad (7)$$

We define control variable as $\mathbf{u}(t)$, that is input signals generated with a FPGA board (LabVIEW, PCIe-7852R LX50) as given by,

$$\mathbf{u}(t) \equiv \begin{bmatrix} u_1(t) \\ u_2(t) \\ u_3(t) \\ u_4(t) \\ u_5(t) \\ u_6(t) \end{bmatrix} \equiv \begin{bmatrix} u_F(t) \\ u_B(t) \\ u_{L1}(t) \\ u_{L2}(t) \\ u_{R1}(t) \\ u_{R2}(t) \end{bmatrix} \quad (8)$$

$\mathbf{u}(t)$ is amplified j -fold and added offset voltage of V_0 with an amplifier, then becomes $\mathbf{V}(t)$ as,

$$\begin{bmatrix} V_F(t) \\ V_B(t) \\ V_{L1}(t) \\ V_{L2}(t) \\ V_{R1}(t) \\ V_{R2}(t) \end{bmatrix} \equiv j \begin{bmatrix} u_F(t) \\ u_B(t) \\ u_{L1}(t) \\ u_{L2}(t) \\ u_{R1}(t) \\ u_{R2}(t) \end{bmatrix} + V_0 \begin{bmatrix} 1 \\ 1 \\ 1 \\ 1 \\ 1 \\ 1 \end{bmatrix} \quad (9)$$

From (7) and (9), enforced displacement $\mathbf{n}(t)$ is described as,

$$\mathbf{n}(t) \equiv \begin{bmatrix} d_F(t) \\ d_B(t) \\ d_{L1}(t) \\ d_{L2}(t) \\ d_{R1}(t) \\ d_{R2}(t) \end{bmatrix} = \frac{j}{h} \begin{bmatrix} u_F(t) \\ u_B(t) \\ u_{L1}(t) \\ u_{L2}(t) \\ u_{R1}(t) \\ u_{R2}(t) \end{bmatrix} + \frac{V_0}{h} \begin{bmatrix} 1 \\ 1 \\ 1 \\ 1 \\ 1 \\ 1 \end{bmatrix} \quad (10)$$

By substituting (10) into (6),

$$\dot{\mathbf{x}}(t) = \mathbf{A}\mathbf{x}(t) + \mathbf{B}\mathbf{u}(t) \quad (11)$$

(11) represents a state equation of the mechanism. Note that offset enforced displacement V_0/h in (10) becomes 0 in (11) because 6 PAs cancel each other's offset displacements out. We define $\mathbf{x}(t)$ and $\mathbf{y}(t)$ as state and control variables respectively. $\mathbf{A}$ and $\mathbf{B}$ are also defined as follows,

$$\mathbf{x}(t) \equiv \begin{bmatrix} x_1(t) \\ x_2(t) \\ x_3(t) \\ x_4(t) \\ x_5(t) \\ x_6(t) \end{bmatrix} \equiv \begin{bmatrix} x_{G2}(t) \\ y_{G2}(t) \\ \theta_{G2}(t) \\ \dot{x}_{G2}(t) \\ \dot{y}_{G2}(t) \\ \dot{\theta}_{G2}(t) \end{bmatrix} \equiv \begin{bmatrix} x_G(t) \\ y_G(t) \\ \theta(t) \\ \dot{x}_G(t) \\ \dot{y}_G(t) \\ \dot{\theta}(t) \end{bmatrix} \quad (12)$$

$$\mathbf{y}(t) \equiv \begin{bmatrix} x_1(t) \\ x_2(t) \\ x_3(t) \end{bmatrix} \quad (13)$$

$$\mathbf{A} \equiv \begin{bmatrix} 0 & 0 & 0 & 1 & 0 & 0 \\ 0 & 0 & 0 & 0 & 1 & 0 \\ 0 & 0 & 0 & 0 & 0 & 1 \\ -\frac{3(k_s+k_L)}{m} & 0 & 0 & -\frac{3c_s}{m} & 0 & 0 \\ 0 & -\frac{3(k_s+k_L)}{m} & 0 & -\frac{3c_s}{m} & 0 & 0 \\ 0 & 0 & -\frac{3(k_s+2k_L)r^2}{2I} & 0 & 0 & -\frac{3c_sr^2}{2I} \end{bmatrix} \quad (14)$$

$$\mathbf{B} \equiv \frac{k_L j}{h} \begin{bmatrix} 0 & 0 & 0 & 0 & 0 & 0 \\ 0 & 0 & 0 & 0 & 0 & 0 \\ 0 & 0 & 0 & 0 & 0 & 0 \\ -\frac{1}{m} & -\frac{1}{m} & \frac{1}{2m} & \frac{1}{2m} & \frac{1}{2m} & \frac{1}{2m} \\ 0 & 0 & \frac{\sqrt{3}}{2m} & -\frac{\sqrt{3}}{2m} & -\frac{\sqrt{3}}{2m} & \frac{\sqrt{3}}{2m} \\ \frac{1}{I} & -\frac{1}{I} & -\frac{1}{I} & \frac{1}{I} & -\frac{1}{I} & \frac{1}{I} \end{bmatrix} \quad (15)$$

Output equation is represented by,

$$\mathbf{y}(t) = \mathbf{C}\mathbf{x}(t) \quad (16)$$

Here, $\mathbf{C}$ is defined as given by,

$$\mathbf{C} \equiv \begin{bmatrix} 1 & 0 & 0 & 0 & 0 & 0 \\ 0 & 1 & 0 & 0 & 0 & 0 \\ 0 & 0 & 1 & 0 & 0 & 0 \end{bmatrix} \quad (17)$$

Because (17) does not have control variable of $u(t)$, the proposed dynamical model is strictly proper.

DESIGN

In Fig. 7, we show the design of an integrated 3DoF inner position sensor with four optical chip encoders (Mercury1540P, MicroE systems). These chip encoders have $0.5\mu\text{m}$ resolution and a maximum measuring speed of 7.2m/s as listed in Table 3. These encoders measure the displacement of a sinusoidal vibration up to 120 kHz with its maximum amplitude of $100\mu\text{m}$. Four chip encoders are fixed to EM-2, and their scales are fixed to EM-1. If one EM is fixed to the floor, the other's motion is measured by the four encoders in the x -, y -, and θ -axes with $0.5\mu\text{m}$ resolution.

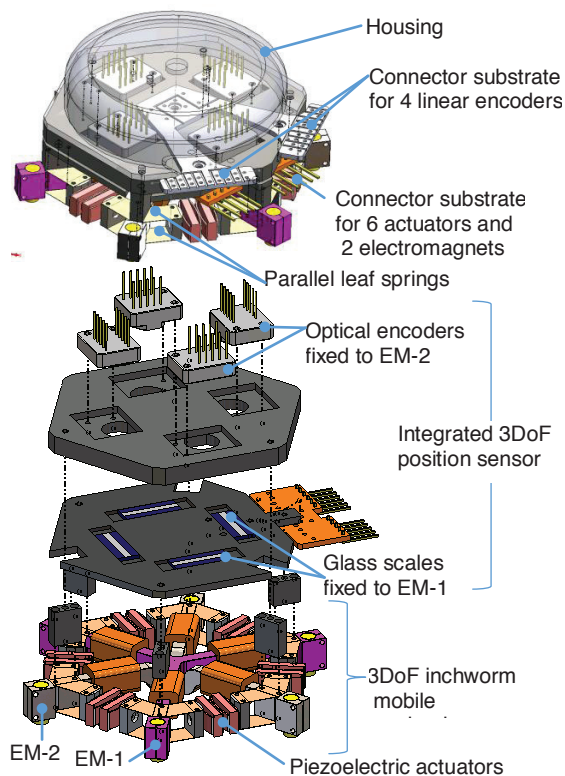


Fig. 7. Assembly drawing

TABLE III
PERFORMANCE OF THE CHIP ENCODER

Quantity	
Resolution	$0.5\mu\text{m}/\text{count}$
Maximum measuring speed	7200 mm/s
Height x Thickness x Length	$12.7 \times 15.2 \times 5.6\text{ mm}$

EXPERIMENT SYSTEM

To confirm the feasibility of the proposed integrated 3DoF inner position sensor, we

developed 3DoF “outer” position sensor which has better measurement resolution of $0.05\mu\text{m}$ than that of the inner sensors with the same arrangement of the encoders as shown in Figs. 8 and 9. In this experiment, we measured the displacement of the free leg with optical linear encoders (MicroE Systems Inc., Mercury3500) fixed to a working table. Here, glass scales were placed on the housing attached to the free EM. Both of the fixed EM and encoders were clamped to the working table with screws. In this setup, we measured displacements of the free EM in x , y , and θ -axis. Input signals were generated with the FPGA board (LabVIEW, PCIe-7852R) and amplified 32-fold with an amplifier.

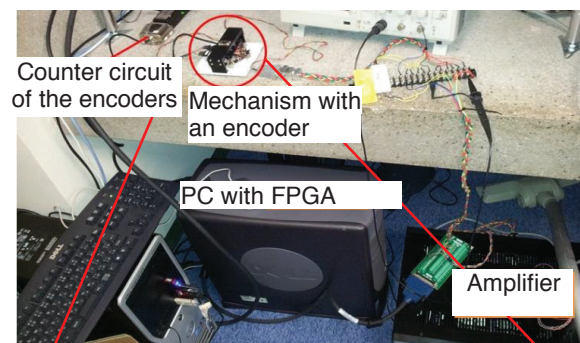


Fig. 8. Experimental

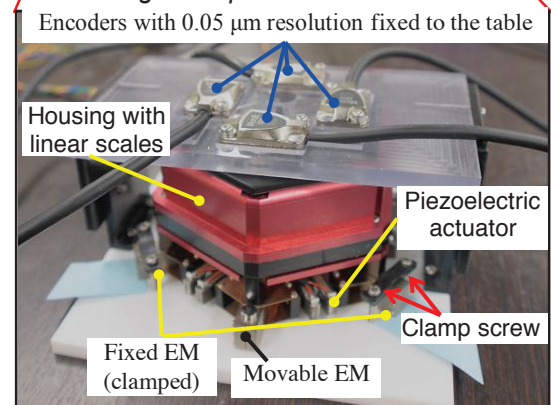


Fig. 9. Close up view of the mechanism with “outer” encoders

EXPERIMENTAL RESULTS

We moved mechanism to goal position ($20\mu\text{m}, 0\mu\text{m}, 0\mu\text{rad}$) and measured the displacement of the free leg with inner position sensor and outer position sensor. Now we just want to confirm the feasibility of the proposed integrated 3DoF inner position sensor, so we ignore position displacement between goal position and sensor position. Fig10,11,12 shows typical plot of the displacement in x -, y -, and θ -axis vs time.

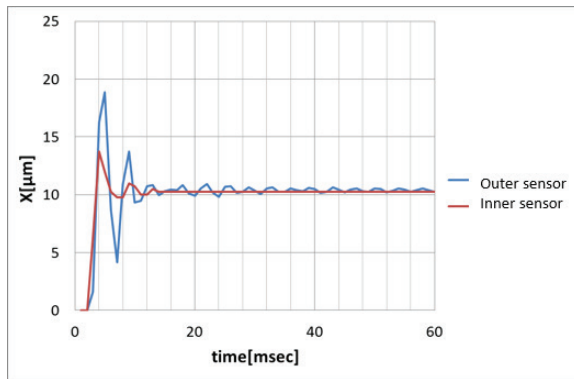


Fig.10 Plot of x vs. time

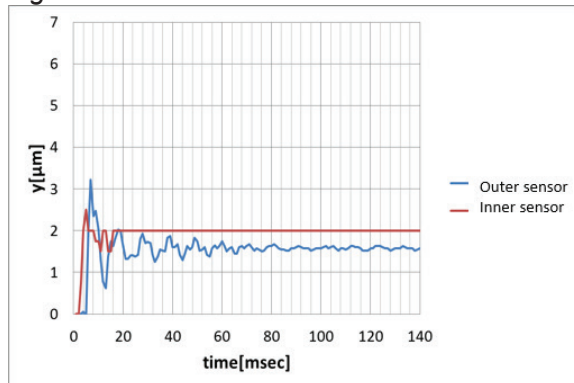


Fig.11 Plot of y vs. time

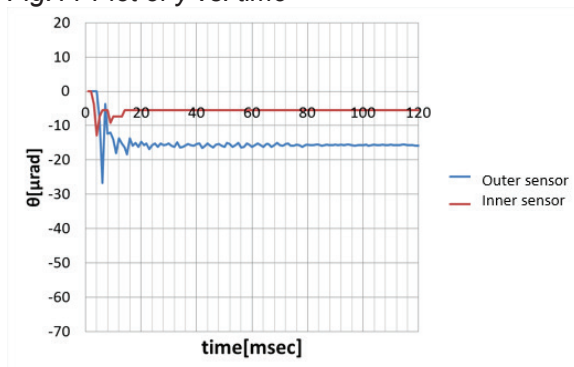


Fig.12 Plot of θ vs. time

displacement between position by inner position sensor and position by outer position sensor are within all measurement resolution ($x = \pm 0.5 \mu\text{m}$, $y = \pm 0.5 \mu\text{m}$, $\theta = \pm 29.3 \mu\text{rad}$)

CONCLUSION

we developed newly proposed design of the integrated 3DoF inner position sensor by 4 encoders with $0.5 \mu\text{m}$ resolution for precise holonomic inchworm robot

.This spec is

- measurement range: $40 \mu\text{m} \times 40 \mu\text{m}$, $\pm 0.04^\circ$
- sampling cycle: 1ms

- positing resolution: $x = \pm 0.5 \mu\text{m}$, $y = \pm 0.5 \mu\text{m}$, $\theta = \pm 29.3 \mu\text{rad}$
- size: $72\text{mm} \times 72\text{mm} \times 28\text{mm}$, weight 85.2g

We are now developing combination of the bang-bang control and 3-axis PID control with the acceleration FF controller to get more accurate and faster positioning for the miniature mobile robot. Final goal of this project is making precise, fast, flexible, energy-efficient, and compact robotic factory organized by these precise miniature robots for chip mounting and bio-medical applications

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DISTANCE MEASUREMENT METHOD USING TWO FIZEAU INTERFEROMETERS AND LASER-BEAM WAVELENGTH-SHIFTING PHENOMENA

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INSTRUCTIONS

There are many optical interferometric techniques wide used to precisely measure distances. Most of the optical interferometric techniques use advanced interferometry to perform well, but their setups are very complex and expensive usually. Whereas, some of the optical interferometric techniques only use basic interferometry and they also perform well. Moreover, their setups are simpler usually. Therefore, this article will focus on distance measurement methods using basic interferometry.

Many distance measurements by using basic interferometry are done by using wavelength-shifting phenomena of laser beams. Kikuta *et al.* [1] used wavelength-shifting phenomena of a current-controlled laser to measure distances smaller than a few centimeters. Kubota *et al.* [2] used an FM (frequency modulation) radar technique to a laser to measure distances of tens of centimeters. Ikezawa *et al.* [3] used a frequency-tunable laser to measure distances between 0.1mm and 10.1mm. Groot and McGarvey [4] used the modulation of fringe contrast of two frequency-tunable lasers to measure distances between 50mm and 200mm. Fischer *et al.* [5] used an FM demodulation technique to a laser to measure distances not bigger than 50mm. Thiel *et al.* [6] used wavelength-shifting phenomena of a current-controlled laser to measure distances up to 40m. Mokdad *et al.* [7] used a frequency-modulated continuous optical wave technique to a tunable laser to measure a distance about 4.7m. Kakuma and Ohba [8] used a frequency-

modulated continuous optical wave technique to a frequency-tunable laser to measure a distance of 100mm. Ogurtsov *et al.* [9] used an all-fiber frequency-shifted feedback laser to measure distances between 3.6m to 4.8m. Although all the above-mentioned methods performed well, they used passive wavelength-shifting phenomena (caused by different electric or mechanic methodss) of lasers, so their costs were higher. Moreover, most of them used Michelson or Mach-Zehnder interferometry [10], so their resistant capacity for system vibrations was lower and very smooth surfaces for reflecting beams were needed. On the other hand, this article proposes a distance measurement method by using active wavelength-shifting phenomena (caused by heat) of lasers. Its cost is lower. Moreover, it uses Fizeau interferometry, so its resistant capacity for system vibrations is higher. The most important feature is that rough surfaces with beam diffusing or scattering phenomena can be measured.

THEORY

Figure 1 shows the setup diagram used in the proposed method [11]. A laser beam is split into two beams with orthogonal polarizations, the transmission beam is P-polarized and the reflection one is S-polarized, by a polarization beam splitter (PBS). The P-polarized beam passes a first non-polarization beam splitter (NPBS 1) and a second non-polarization beam splitter (NPBS 2), and illuminates on a mirror. Both the component NPBS 1 and the component NPBS 2 can reflect the P-polarized beam, but only the beam reflected by the

component NPBS 2 is needed here. The mirror also reflected the P-polarized beam. The beam reflected by the component NPBS 2 and the beam reflected by the mirror are combined to interfere with each other. The combined beams are reflected by the component NPBS 1 to illuminate on a first charge-coupled device (CCD 1) to form a first interference pattern. On the other hand, the S-polarized beam is reflected by a third non-polarization beam splitter (NPBS 3) and passes a fourth non-polarization beam splitter (NPBS 4), and illuminates on an object to be measured. Both the component NPBS 4 and the object can reflect the S-polarized beam, but the beam reflected by the component NPBS 4 possesses parallel waves and the beam reflected by the object possesses diffusing or scattering waves. The beam reflected by the component NPBS 4 and the beam reflected by the object are combined to interfere with each other. The combined beams pass the component NPBS 3 to illuminate on a second charge-coupled device (CCD 2) to form a second interference pattern.

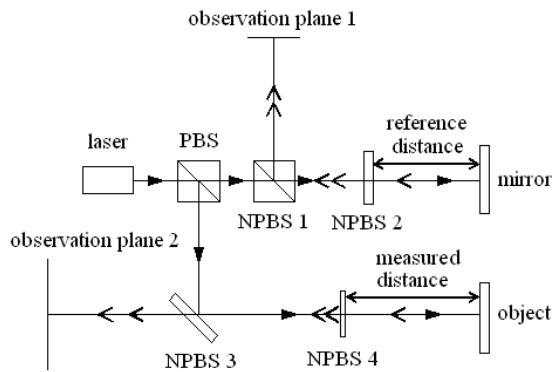


FIGURE 1. Setup diagram used in the proposed method.

Both the components PBS and NPBS 1 are cubic beam splitters, and the components NPBS 2 and NPBS 3 are plate beam splitters. Whereas, the component NPBS 4 is a special plate beam splitter with a thickness of 0.2mm and with two anti-reflection coating sides. The reason for the thickness of 0.2mm is to much reduce the optical path differences of beams (for the second interference pattern) reflected by the component NPBS 4. The reason for two anti-reflection coating sides is to much reduce the intensities of the two beams (on the second interference pattern) reflected by the component

NPBS 4 to approach the intensity of the very weak beam (on the second interference pattern) reflected from the object to make the contrast of the second interference pattern higher.

The first interference pattern is formed by using a conventional Fizeau interferometer because both the two beams reflected by the component NPBS 2 and the mirror possess parallel waves. Therefore, the interference pattern is formed by two plan-wave beams and it includes equal-separated straight interference fringes. On the other hand, the second interference pattern is formed by using a modified Fizeau interferometer because the beam reflected by the component NPBS 4 possesses parallel waves and the beam reflected by the object possesses diffusing or scattering waves. When the distance between the component NPBS 4 and the illuminated area of the measured object is small (e.g. 0.5m), the interference pattern is formed by a plan-wave beam and an irregular-wave beam and it includes irregular, useless interference dots. When the distance between the component NPBS 4 and the illuminated area of the measured object is big (e.g. 2m), the interference pattern is formed by a plan-wave beam and a quasi-spherical-wave beam and it includes concentric interference fringes.

For the first interference pattern, the beam reflected by the component NPBS 2 is called the *reference beam* and the beam reflected by the mirror is called *object beam*. The distance between the back surface (with a coating layer for the 50/50 beam-splitting feature) of the component NPBS 2 and the front surface (with a coating layer for reflection) is called *reference distance* and it is denoted by L_1 . Therefore, the optical path difference P_1 between the reference beam and the object beam equals $2L_1$ and the phase difference θ between the two beams is

$$\theta = 2\pi \frac{P_1}{\lambda} = 4\pi \frac{L_1}{\lambda}, \quad (1)$$

where λ is the wavelength of laser beams.

For the second interference pattern, the beam reflected by the component NPBS 4 is called *reference beam* and the beam reflected by the object is called *object beam*. The distance between one of the two surfaces (with coating layers for anti-reflection) of the component

NPBS 4 and the illuminated area of the object is called *measured distance* and it is denoted by L_2 . Therefore, the second optical path difference P_2 between the reference beam and the object beam equals $2 L_2$ and the phase difference ϕ between the two beams is

$$\phi = 2\pi \frac{P_2}{\lambda} = 4\pi \frac{L_2}{\lambda}. \quad (2)$$

Because the laser tube of the laser is sensitive to the temperature and the temperature always keeps increasing after the laser starts, the wavelength λ keeps changing after the laser starts. Assume the wavelength at time t_1 is λ_1 and the wavelength at time t_2 is λ_2 , then their corresponding phase differences for the first interference pattern are respectively

$$\theta_1 = 4\pi \frac{L_1}{\lambda_1}, \quad (3)$$

and

$$\theta_2 = 4\pi \frac{L_1}{\lambda_2}. \quad (4)$$

Therefore, the difference $\Delta\theta$ between the two phase differences θ_1 and θ_2 is

$$\Delta\theta = \theta_2 - \theta_1 = 4\pi L_1 \frac{\lambda_1 - \lambda_2}{\lambda_1 \lambda_2}. \quad (5)$$

On the other hand, the corresponding phase differences of the wavelengths λ_1 and λ_2 for the second interference pattern are

$$\phi_1 = 4\pi \frac{L_2}{\lambda_1}, \quad (6)$$

and

$$\phi_2 = 4\pi \frac{L_2}{\lambda_2}. \quad (7)$$

Therefore, the difference $\Delta\phi$ between the two phase differences ϕ_1 and ϕ_2 is

$$\Delta\phi = \phi_2 - \phi_1 = 4\pi L_2 \frac{\lambda_1 - \lambda_2}{\lambda_1 \lambda_2}. \quad (8)$$

So, the ratio of the two differences $\Delta\theta$ and $\Delta\phi$ can be derived as

$$\frac{\Delta\phi}{\Delta\theta} = \frac{L_2}{L_1} \quad (9)$$

or

$$L_2 = \frac{\Delta\phi}{\Delta\theta} L_1, \quad (10)$$

i.e. the measured distance L_2 only depends on the reference distance L_1 and the ratio $\Delta\phi / \Delta\theta$.

The measurements for the two differences $\Delta\theta$ and $\Delta\phi$ are done respectively according to the interference fringe shifting amounts (the unit is *fringe*), not the phase shifting amounts (the unit is *radian* or *degree*), of the first and second interference patterns. The interference fringe shifting amount of an interference pattern is determined by using the correlation of two images [12] taken from the interference pattern, one is taken at time t_1 and the other is taken at time t_2 . Of course, the interference fringe shifting amount directly determined by the correlation operation is between 0 and 1, so the real shifting amount is the addition of the determined amount and an integer which can be easily derived by continuously tracing the shifting path of an interference fringe.

EXPERIMENTS

Using the setup as shown in Figure 1, a distance of 300mm (the measured distance) was measured. The measured object was a metal plate with surfaces after anodizing. The reference distance was set as 450mm. Both the reference distance and the measured distance were set according to a steel vernier caliper with 600-mm measurement capacity. Figure 2 shows the selected local area (the white rectangle with 64×128 pixels) in the first interference pattern for the correlation operation of two images. Figure 3 shows the selected local area (the white rectangle with 128×64 pixels) in the second interference pattern for the correlation operation of two images.



FIGURE 2. Selected local area (the white rectangular area) in the first interference pattern for the correlation operation of two images.

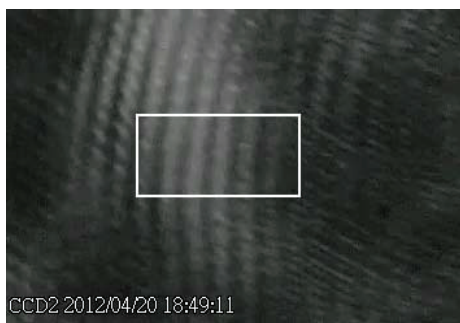


FIGURE 3. Selected local area (the white rectangular area) in the second interference pattern for the correlation operation of two images.

A 40-mW diode pumped solid state (DPSS) laser with a nominal wavelength of 532nm was used. Because the wavelength shifting phenomena of the laser decayed with the time and the phenomena were not very stable within 1.0s after the laser started. Therefore, each measurement was done from the time of 1.0s to the time of 1.5s after the laser was restarted.

Thirty times of measurements had been done. The derived values for the measured distance were 301.4, 299.2, 300.6, 300.2, 298.4, 300.3, 299.5, 299.8, 298.7, 298.7, 298.3, 300.1, 300.6, 300.8, 301.0, 298.2, 300.4, 298.8, 301.3, 299.1, 298.9, 300.2, 299.6, 298.4, 300.6, 301.4, 298.6, 300.3, 301.1, and 297.6mm. The average value of the derived values was 299.7mm and the standard deviation of the derived values was 1.1mm. Therefore, the average error percentage was about 0.1%.

DISCUSSIONS

The two differences $\Delta\theta$ and $\Delta\phi$ must be derived at the same time duration (from time t_1 to time t_2), so simultaneously taking the image of the first interference pattern and the image of the second interference pattern is important.

For scattering or diffusing waves to become quasi-spherical waves, the scattering or diffusing distance must be long enough, i.e. longer than a threshold distance L_s . In fact, the determination of L_s depends on the diameter of the illuminated area on the measured object. [13]

Because the optical path difference P of two interference beams is usually not small for the Fizeau interferometry, the coherence length L_c of the laser used for measurements must be bigger than P to make interference fringes obvious enough. The coherence length L_c of the DPSS laser used in this article is longer than 2m, so both the reference distance L_1 and the measured distance $L_2 (= P_2 / 2)$ can be longer than 1m.

CONCLUSIONS

This article proposes a new distance measurement method using two Fizeau interferometers and laser-beam wavelength-shifting phenomena caused by the heat expansion of a laser tube. The first Fizeau interferometer can form straight, periodical interference fringes and the second Fizeau interferometer can form concentric, periodical interference fringes. According to the fringe shifting amounts of the two sets of interference fringes and a reference distance set in the first Fizeau interferometer, a measured distance set in the second Fizeau interferometer can be derived.

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